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# A FUNDAMENTAL INVESTIGATION INTO THE MACHINING OF HIGH STRENGTH STEEL

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THE COLLEGE OF AERONAUTICS
CRANFIELD

THESIS

# A FUNDAMENTAL INVESTIGATION INTO THE MACHINING OF HIGH STRENGTH STEEL

W. C. HOLTON, LT., U.S. NAVY

Thesis
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# TABLE OF CONTENTS

page

Summary 1

Introduction 9

Historical Sketch 11

Generalized Solution of the Shear Angle Relationship 35

Prediction of Tool Life 42

- Introduction 42
- Method of Test 49
- Results and Discussion 53
- Conclusions 57

Milling Characteristics of DTD 331 59

- Introduction 59
- Method of Test 61
- Results and Discussion 63
- Conclusions 68

Investigation into Graphical Technique of Determining No Wear Forces 69

- Introduction 69
- Method of Test 72
- Results 72
- Conclusions 78

# TABLE OF CONTENTS

| | | page |
|---|---|---|
| Appendix (a) | (a) 1 | 79 |
| Appendix (b) | (b) 1 | 102 |
| Appendix (c) | (c) 1 | 111 |
| Appendix (d) | (d) 1 | 115 |
| Acknowledgment | | 119 |
| Bibliography | | 120 |

# SUMMARY

The development of the first printing press, electric light, and guided missile depended on the solution of two common problems: the finding of metal alloys with the desired characteristics, and the development of methods of forming and fabricating them.

One of the most common and versatile methods of forming metals is the machining of them with rigid, sharp cutting tools. Although the result, the removal of metal, is obvious, the mechanism of metal cutting is complex and not thoroughly understood. The adaptation of machining to new problems and the development of the most effective and economical use of the cutting tools therefore demands constant and continuing research.

One of the main requisites for the economical use of machine tools is a knowledge of the life of the tool, expressed either in length of time or volume of material removed before failure or malfunction, which can be expected under any specified cutting conditions. With a knowledge of tool life the production engineer can plan the sequence of manufacture for the most efficient use of men, machines, and tools.

Research into the problem of predicting tool life

was undertaken for this thesis.

A general review of the mechanical, thermal, and empirical analyses of metal cutting was made as a background to the problem of predicting tool life.

A generalized solution to the problem of the relationship of the shear angle, rake angle, and friction angle was developed combining the solutions of Lee and Shaffer; and Shaw, Cook and Finnie into the formulae:

$$\phi = 45^\circ + \eta' - \beta + \alpha + \theta$$

$$\frac{\sigma_s}{\tau_s} = \frac{1 + 2\theta + \sin 2\eta'}{\cos 2\eta'}$$

Where $\phi$ = shear angle; $\beta$ = friction angle; $\alpha$ = rake angle; $\theta$ = an angle of variable stress near the tool point; $\eta'$ = the angle between the plane of maximum shear stress and the shear plane; $\sigma_s$ = normal stress on the shear plane; $\tau_s$ = shear stress on the shear plane.

A review of past analyses and experiments in metal cutting reveal that the factors determining tool wear and tool life, that is, tool strength and hardness, workpiece hardness and crystallographic structure, workpiece strain hardening characteristics and recrystallization characteristics, weldability of tool and workpiece, are all directly influenced by temperature. The high sources of thermal energy in metal cutting: plastic strain at the shear plane and friction at the tool face, point to the possibility

of correlating the temperature at the interface of the tool and workpiece with tool life.

Experiments were conducted using high speed steel tools containing 18 % tungsten, 4 % chromium, and 1 % vanadium on a nickel-chromium-molybdenum steel (S-96) in the turning operation. Tool life, interface temperature, and cutting force were measured for various feeds per revolution, cutting speeds, and side rake angles.

Comparative tool life, interface temperature, and force measurements were then taken for the same tools on a stronger nickel-chromium steel (DTD-331).

The results indicate that, for a high speed steel tool cutting high strength steel, for a given tool shape, the interface temperature is a true index of tool life in minutes. That is, that a given temperature developed by various combinations of feed and speed determines the same tool life within the accuracy of temperature measurements.

The temperature tool life in minutes relationship did not vary appreciably for different workpiece materials. However it must be recognized that the two workpiece materials tested were similar in composition and structure.

The tool life temperature relationship varied with tool shape indicating lower tool life for a given temperature with larger side rake angles.

The interface temperature and the metal removed per tool life were related in such a way that:

(1) There exists for a given temperature an optimum speed and feed for maximum metal removal.

(2) A given temperature is reached in a higher strength steel at a lower speed than in the weaker steel, therefore, the material removed before failure is smaller, although tool life in minutes is the same.

(3) Although, for a given temperature, tool life in minutes decreased with increasing side rake, the material removed before failure did not necessarily decrease. The cutting speed required to attain a given temperature varied with the side rake angle. Twenty-five degrees side rake accommodated the highest speed for a given temperature and provided the greatest metal removal. An increase or decrease in side rake from twenty-five degrees resulted in a decreased speed and a decreased metal removal for a given temperature.

The tool life temperature relationship determined for the twenty degree side rake angle was $\omega T^{\frac{1}{4}} = 122$, where $\omega$ = temperature in degree centigrade, and $T$ = tool life in minutes.

The nature of this function reveals the main drawback to the adoption of interface temperature measurement for the prediction of tool life: any error in measure-

ment of temperature will be raised to the fourteenth power in determining the corresponding tool life. The adoption of any facile temperature measurement method with an error of more than two per cent would result in tool life prediction errors in the order of thirty-two per cent.

One of the main problems in aircraft production is the economical machining of the high strength steel alloys required in high performance aircraft.

Most of the information required for the choice of optimum cutting conditions for these steels is obtained by experimental machining tests on them.

Tests for this purpose were carried out on DTD-331 nickel-chromium steel with one inch end mills containing 6 % tungsten, 5 % molybdenum, and 2 % vanadium.

The salient findings for this type of milling on DTD-331 are:

(1) That maximum tool life at every rotational cutter speed occurs at .004 inches advance per tooth (.0013 inches maximum chip thickness).

(2) That maximum metal removal before tool failure at every rotational cutter speed occurs at a table advance rate of 14 3/4 inches per minute.

The last finding is not discernable in any of the routine methods of plotting tool life data (such as cutting

speed versus tool life). However, it becomes obvious when the metal removed per tool life is plotted against tool life in minutes. It is suggested that this type of plot is the most effective means of presenting tool life data.

Because of the expense in time, tools and material of investigations of this type to determine optimum cutting conditions for turning, milling, drilling etc., there is a recognized need for some means of using the results of say some simple turning tests to determine other machining characteristics such as milling characteristics.

In order to determine the feasibility of relating turning life data with milling life data, comparative turning tests were conducted on DTD 331 with tool bits of the same composition as the end milling cutter.

A general formula was derived for determining end milling cutting speed-tool life curves from turning cutting speed-tool life curves of the form:

$$V_m T_m^{n_T f^{-x}} = C_T f^{y}$$

where $V_m$ = milling cutting speed (peripheral speed of cutter), $T_m$ = milling tool life (equivalent continuous cutting time per tooth), $n_T$ = slope of cutting speed-tool life curve for turning, $f$ = feed, $C_T$ = turning cutting speed for one minute tool life, and x and y are exponents

to be found for each particular case. For these particular tools cutting DTD 331, x and y were found to be $\frac{1}{75}$ and $\frac{1}{15.5}$ respectively.

In conducting experiments in machining where force measurements are taken on a tool of a particular shape, care must be taken to insure that the forces determined are those for the tool in the unworn condition. An investigation was made of the validity and necessity of a graphical method of determining the forces on an unworn tool.

The method investigated was based upon the assumption that force measurements cannot be taken for at least ten seconds after the start of a cut during which time appreciable wear has taken place. By plotting force versus wear and extrapolating to zero wear, a no wear force was obtained.

If these conditions apply it was found that the graphical method of extrapolation is a valid means of determining the no-wear forces.

However, it was found that accurate force measurements could be taken in two to three seconds, before wear measurable by microscope or force increase had taken place. The measured force for no wear coincided with the graphically determined value, within the accuracy of the force measurements. It was concluded that there is no apparent necessity for employing a graphical method for determining

a value which can be measured directly.

# INTRODUCTION

The problems that are considered in the research described in the following pages include the prediction of tool life for high speed steel tools, the determination of the end milling characteristics of nickel-chromium steel DTD 331, and the investigation of a graphical method of determining tool forces for an unworn tool.

The method of attack on the first problem consisted in an extensive reconnaissance into the fields of past research and analysis of the orthogonal cutting process, the process most usually investigated in a mechanical or thermal analysis of metal cutting.

"Orthogonal cutting is defined as cutting by a tool having a plane face and a single straight cutting edge, set perpendicular to the direction of relative motion of tool and workpiece, and generating a plane surface parallel to the original plane surface of the workpiece." In other words, its analogous to scraping paint with a screwdriver with the edge held exactly normal to the direction of motion. The turning operation with a straight edged single point tool held perpendicular to the workpiece with large depth of cut in relation to feed approximates orthogonal cutting.

The findings of this reconnaissance are outlined in the historical sketch on the following pages.

A generalized solution of the mechanical analysis of the shear plane angle, friction angle, and rake angle relationship is then developed.

The actual approach to the problem of predicting tool life is made by experiment into the temperature tool life relationship.

The determination of end milling characteristics is through the measurement of tool life and metal removal for various cutter rotational speeds and rates of advance per tooth.

The investigation into the graphical determination of the cutting forces on an unworn tool is by tool force, time and coincident tool wear measurement and plotting.

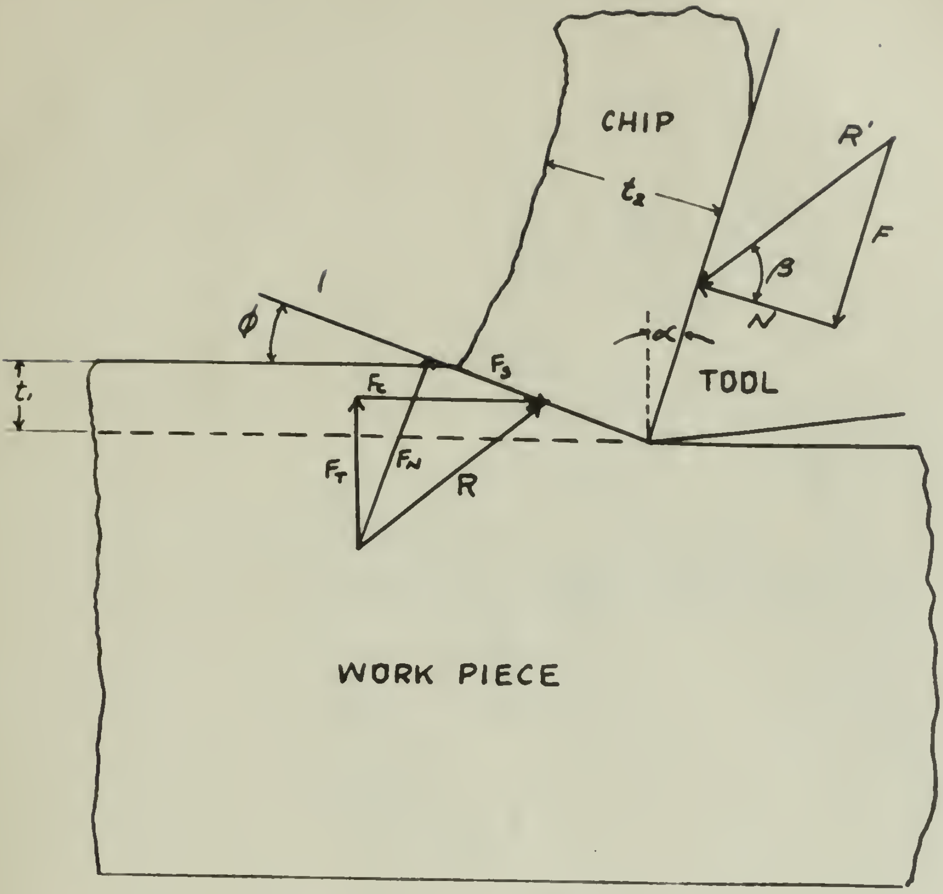
Analysis of Merchant:

The first light to penetrate the obscurity surrounding the mechanics of metal cutting was turned on by Vaino Púspanan of Finland in 1937. Unfortunately for the English speaking world he reported what he saw in Finnish, and it was not until 1945 that Dr. Eugene Merchant found the same switch and illuminated the fundamental mechanics in an almost precise unknowing duplication of Púspanan's reasoning.

The findings of these two men in no way invalidated the excellent empirical laws formulated by F. W. Taylor and others. They did, however, form the basis for all subsequent advances in the fundamental scientific theory of metal cutting.

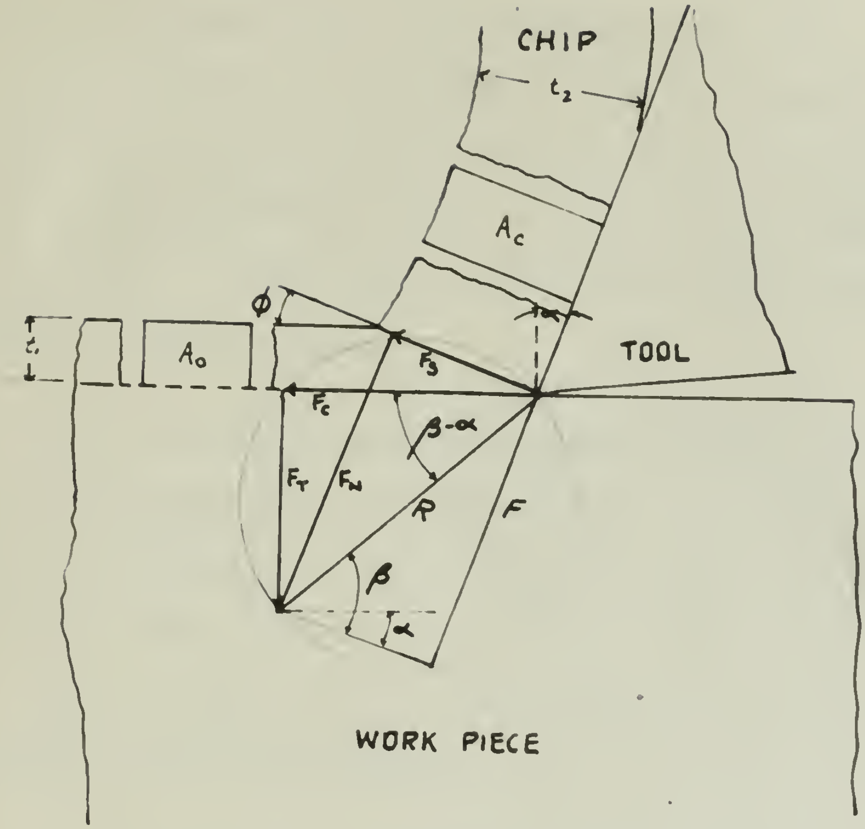
Briefly, Merchant reasoned that if a continuous unaccelerating chip were to be separated from the workpiece by a cutting tool then the forces on the chip imposed by the tool and workpiece must be in equilibrium.

On these premises he built the classical graphical representation of the forces as shown in fig. 1 and 2, where the vector sum of N, the normal force of the tool on the chip, and F, the friction force, should equal the



TOOL FORCE DIAGRAM

Figure 1



MERCHANT'S DIAGRAM

Figure 2

vector sums of $F_s$, the shear force, and $F_n$, the normal force on the shear plane.

He further reasoned that the shear strength of the metal being cut is a true constant, invariant with respect to the shear angle, $\phi$. He further reasoned that the physical properties governing the plastic behaviour of the work material determine what value $\phi$ will assume, for any given values of friction angle, $\beta$, and rake angle, $\alpha$. By applying the principle of minimum energy he reasoned that the angle $\phi$ will assume a value to make the total work done in cutting per unit volume a minimum.

Since the cutting speed is considered constant, the conditions for minimum energy were obtained by setting the first derivative of cutting force, $F_c$, with respect to $\phi$ equal to zero. That is (fig. 2)

$$F_c = \tau_s A_0 \sin(\beta - \alpha) / \sin\phi \cos(\phi + \beta - \alpha) \qquad (1)$$

since $\tau_s$ is considered constant

$$\frac{dF_c}{d\phi} = -\tau_s A_0 \cos(\beta - \alpha)\left[\frac{\cos\phi\cos(\phi + \beta - \alpha) - \sin\phi\sin(\phi + \beta - \alpha)}{\sin^2\phi\cos^2(\phi + \beta - \alpha)}\right] = 0 \qquad (2)$$

or $\cos(2\phi + \beta - \alpha) = 0$ (3)

and $2\phi + \beta - \alpha = 90^\circ$ (4)

Three major assumptions were made:

(1) That the shear angle, $\phi$, is independent of the friction angle, $\beta$.

(2) That the shear stress is a constant maximum in

the direction of the shear plane.

(3) That the resultant force, R, is independent of the shear angle.

Unfortunately none is exactly true and experimental results do not support Merchant's findings that $2\phi + \beta - \alpha = 90°$.

He of course was one of the first to become aware of this variance and to explain it he adopted some of Bridgman's findings that shear stress varies with compressive stress in some linear manner as in fig. 3. That is, that $\tau_s = \tau_0 + k\sigma_s$ (5)

Again using his graphical representation in fig. 2, it is evident that

$$\sigma_s = \tau_s \tan(\phi + \beta - \alpha) \qquad (6)$$

substituting this in the equation (5) gives

$$\tau_s = \tau_0 - k\tau_s \tan(\phi + \beta - \alpha) \qquad (7)$$

or $\tau_s = \tau_0 / \left[1 - k\tan(\phi + \beta - \alpha)\right]$ (8)

By substituting this value for $C_s$ in formula (1) for cutting force one gets

$$F_c = \tau_0 A_0 \sin(\beta - \alpha) / \left[1 - k\tan(\phi + \beta - \alpha)\right] \sin\phi \cos(\phi + \beta - \alpha) \qquad (9)$$

Again differentiating this with respect to $\phi$ and setting this equal to zero for minimum energy one gets that

$$\cos(2\phi + \beta - \alpha) = k \sin(2\phi + \beta - \alpha) \qquad (10)$$

or $\cot(2\phi + \beta - \alpha) = k$ (11)

or $2\phi + \beta - \alpha = \cot^{-1} k = C$ (12)

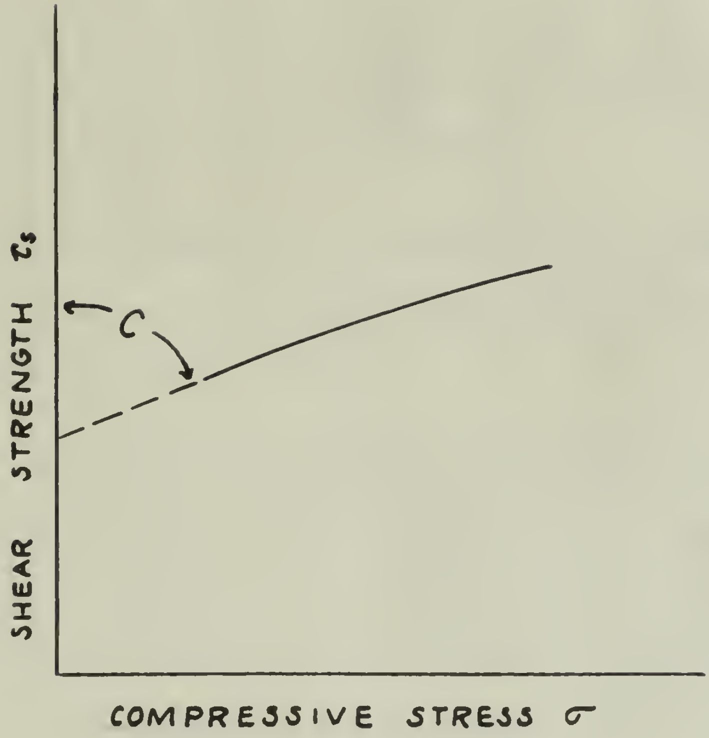


Figure 3

Merchant pointed out that this analysis does not take into account the effect on the constants $\tau_0$ and $k$ of temperature, rate of shear, or the effect of strain on the shear stress.

In other words he assumed perfect plasticity, that is, that the change in shear strength for any change of shear strain is zero i.e. $\frac{d\tau}{d\epsilon} = 0$. He, therefore, claimed this as only a first approximation.

As he expected, his analysis does not exactly explain experimental results, nor do experimental results confirm his analysis.

An interesting indication of the impact of Merchant's analysis is that practically all subsequent investigations on the mechanics of metal cutting begin with a refutation of his assumptions: Chao and Bisacre (39); Lee and Shaffer (36); Chao, Trigger and Zylstra (30); Shaw, Cook and Finnie (22); Drucker (10); Stabler (42), and others.

They all agree that:

(1) The theory of minimum energy is not necessarily observed by natural process.

(2) The friction angle is not independent of the shear angle.

(3) Although the normal stress on the shear plane could account for the capacity of the chip to withstand high strain rates without rupture, it does not induce

the high shear stress on the shear plane met in metal cutting.

Several disputed that there is any relation between the slope of the shear stress versus normal stress curve for a material and the value of $2\phi+\beta-\alpha$ in the cutting operation. Merchant maintained that there is a relation at high normal stresses.

However, an interesting insight into this problem, even to the whole problem of the mechanics of metal cutting, might be gained by quoting Merchant:

"The only method which the present investigation offers for determining this constant C is initial cutting tests on the material with measurement of both forces and chip geometry."

In other words if his constant $C = 2\phi+\beta-\alpha$ is determined from metal cutting data it can be used thereafter as a first approximation for the value of $2\phi+\beta-\alpha$ in the cutting of that material. Merchant claims that this is in fact the arc cot of the shear stress normal stress curve for high stresses; but he admits it cannot be proven by any separate test, and he offers machining data as the only source for determining it.

Analysis of Shaw:

M. C. Shaw (7) attempted a direct extension to

Merchant's theory.

He contended that the assumption of plasticity for metal cutting was in error, that the cutting stress-strain relationship of the material is not unlike that determined in a quasi-static test. That, as a result of this, strain hardening during the chips passage through the shear plane increases the shear strength of the chip to the values encountered in metal cutting.

By the metallurgical theory of deformation along slip planes caused by an orderly array of weak spots, he obtained an expression for slip such that

$$S = \frac{a^2 \sin\phi}{t} e \qquad (13)$$

Where S slip; a = a constant; e = strain; t = undeformed chip width.

By adding this term to Merchant's formula for shear strength (form. 5) he obtained the expression for shear strength

$$\tau_s = \tau_0 + k\sigma + \frac{As}{2} = \tau_0 + k\sigma + Aa^2 \frac{\sin^2\phi}{2t} e$$

Where A = a constant; $Aa^2$ was defined as a strain hardening constant of the material.

Just as with Merchant's analysis good correlation is obtained if the value of the above constants are obtained from machining data.

However, similarly, the physical theory was inconsistent with the observations of others.

Some contend that the rate of strain in metal cutting, of the order of $10^3$ to $10^6$ per second, when compared with the velocity of strain propagation, is so large as to preclude any effect from strain hardening during shear (39). Shaw persists that slip or shear distortion, in effect, is strain hardening (30) and that no time is required. Therefore, strain hardening does take place during the interval of shearing strain.

Others say that, although strain hardening does take place during shear, its effect is counteracted by an increase in temperature during shear, which reduces shear strength in about the same proportion, so that any strain hardening is ineffective.

Whatever the explanation, there is agreement that the physical justification for Shaw's analysis is not demonstrable.

Analysis of Lee and Shaffer:

E. H. Lee and B. W. Shaffer took the next step of actually applying the principles of ideal plastic flow to the problem of metal cutting.

They assumed for the plasticity conditions that:

(1) The work material behaves as an ideal plastic.

(2) The shear plane is the plane of maximum shear stress.

(3) That a uniform stress field exists in the vicinity of the tool point.

(4) That at some plane there will be zero stress.

The boundary velocity conditions were:

(1) The material ahead of the tool is rigid and at rest.

(2) The tool is moving with uniform velocity.

(3) The chip must leave the plastic region as a rigid body.

The uniform stress field, depicted in figure 4 was represented by a single Mohr's Circle stress diagram fig. 5.

With the maximum shear stress assumed at the shear plane, the plane of zero stress must be at an angle of 45° from the shear plane because of the double angle characteristics of Mohr's Circle.

From the Mohr's Circle one sees that

$$\eta + \beta = 45^\circ$$

From the stress field it is evident that

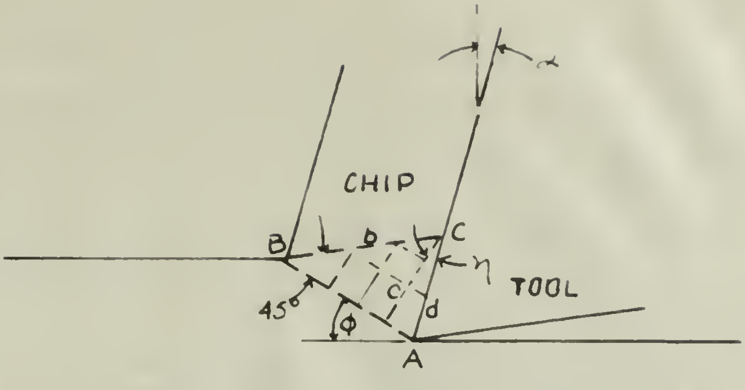
$$\phi = \eta + \alpha$$

or

$$\phi = 45^\circ - \beta - \alpha$$

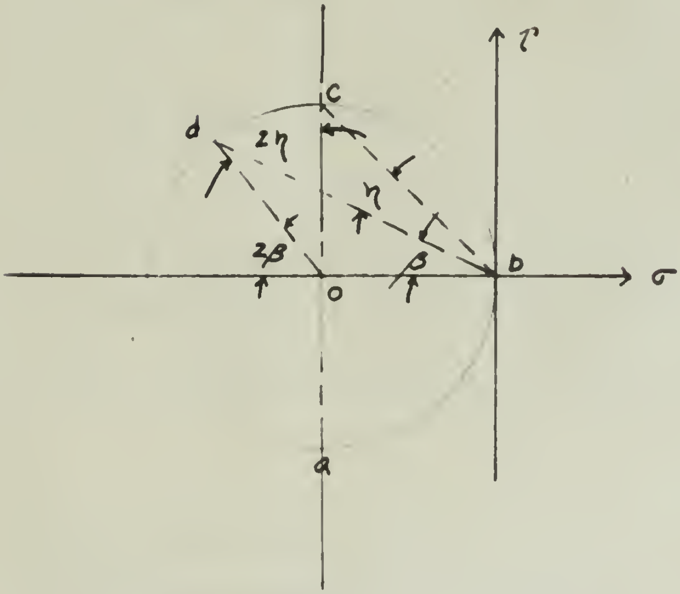
However, from the Mohr's Circle it is noticed that this solution requires that $\frac{\sigma_s}{\tau_s} = 1$, that is that the normal stress equals the shear stress on the shear plane, a limitation which is not observed by machining stresses.

In order to obtain a flexible relationship between



STRESS FIELD AT TOOL POINT

figure 4.



MOHR'S CIRCLE DIAGRAM

figure 5.

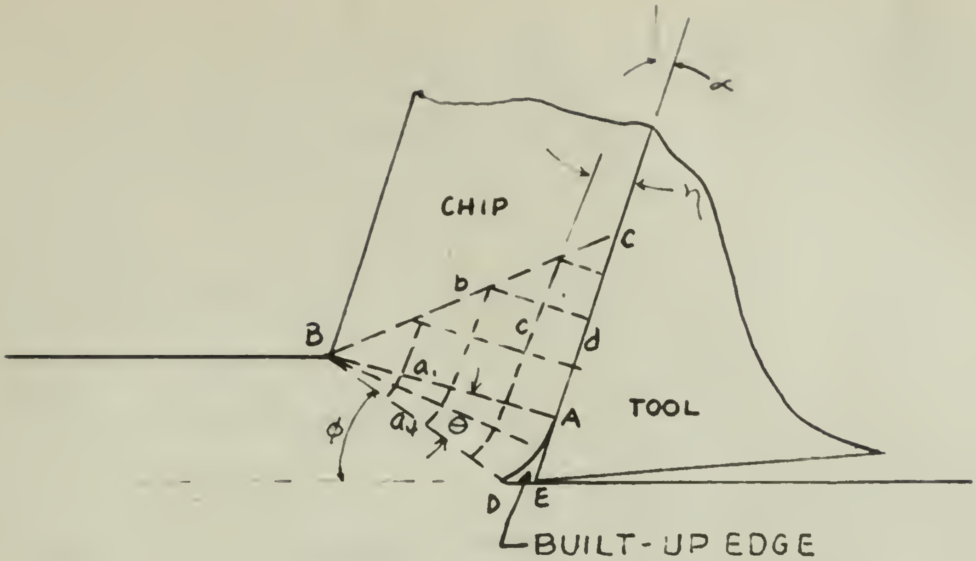
the normal and shear stresses Lee and Shaffer introduced a field of varying stress. Maintaining that the shear plane was still a plane of maximum stress, they rotated the assumed constant stress area up through an angle, $\theta$, sufficient to give the required rationof normal stress to shear stress $\frac{\sigma}{\tau}$. The physical justification for the introduction of this mathematical manipulation was, according to Lee and Shaffer, a built up edge on the cutting point of the tool equal to the arc subtended by the angle $\theta$.

Consequently area ADB then became a field of varying stress having radial lines of constant maximum shear stress and circular arcs of varying normal stress as in fig. 6. The normal stress varies according to the angle turned through. That is, for any given angle, $\delta$, the normal stress varies an amount equal to $\tau_m 2\delta$. Therefore, each radial in the area ADB has a separate Mohr's Circle diagram with the $\tau$ axis displaced an amount equal to $2\tau_m\delta$ fig. 7.

The rigid region ABC is still in a state of uniform stress and can be represented by a single Mohr Circle diagram.

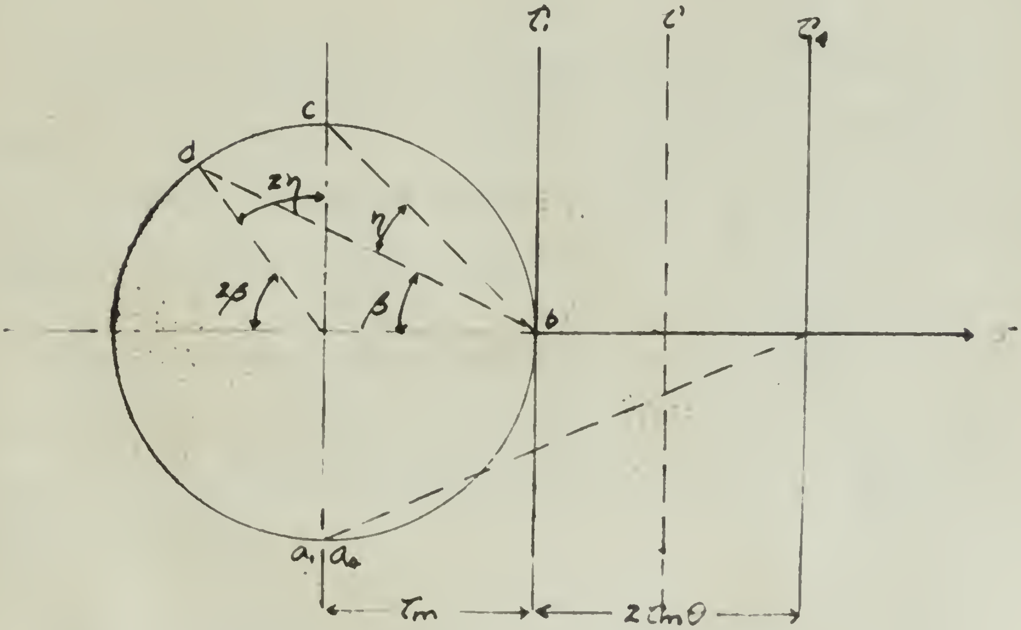
From the Mohr's Circle for the shear plane radial (fig. 7), $\delta = \theta$, it is evident that the value for the normal stress is

$$\sigma_s = \tau_m(1+2\theta). \tag{15}$$



STRESS FIELD AT TOOL POINT

figure 6.



MOHR'S CIRCLE DIAGRAM

figure 7.

where $\tau_m$ = the maximum shear stress. Since the shear stress on the shear plane is assumed equal to the maximum shear stress, that is,

$$\tau_s = \tau_m \tag{19}$$

one can see that, for the shear plane,

$$\frac{\sigma_s}{\tau_s} = 1 + 2\theta \tag{20}$$

From fig. 6 it is evident that

$$\phi = \theta + \alpha + \eta \tag{21}$$

Since                 from fig. 7, then

$$\phi = 15^\circ + \theta - \beta + \alpha \tag{22}$$

which is the Lee and Shaffer solution of the relationship between the shear angle, built-up edge angle, friction angle, and rake angle.

The actual quanitities of the relationship are obtained by measuring the forces and shear angle or chip geometry during a cutting operation. Then by the use of a Mohr's Circle or equations 20 and 22 one can obtain values for $\theta$ and $\beta$ .

Although this analysis affords better understanding of the metal cutting process, it does not yield a means of predicting the shear angle relationship from other than measured metal cutting data.

And, as in previous analyses, the physical basis for their solution is inconsistent with the observations of others.

Lee and Shaffer predict a built-up edge in many cases when none can be found by careful photomicrograph (22). Thus they must fall back upon a theory of an "ideal built-up edge", that is, one which isn't there.

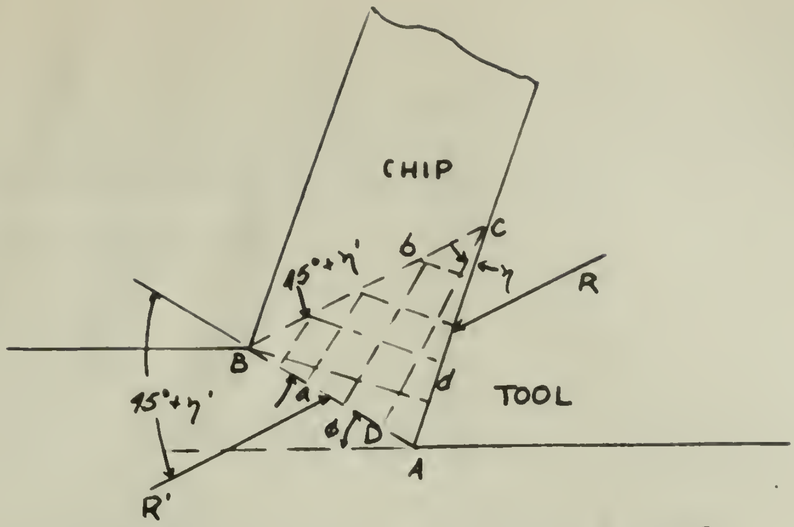
Analysis of Shaw, Cook, and Finnie:

In order to overcome this irregularity Shaw, Cook, and Finnie (22) introduced a theory of effective hardness.

They used the analysis of Lee and Shaffer to obtain a rigid area of constant stress as in Fig. 5. Whereas, Lee and Shaffer maintained that the shear plane was a plane of maximum shear stress, Shaw, Cook and Finnie contended that this was not necessarily true. They contended that the shear plane was some angle $\eta'$ from the plane of maximum stress.

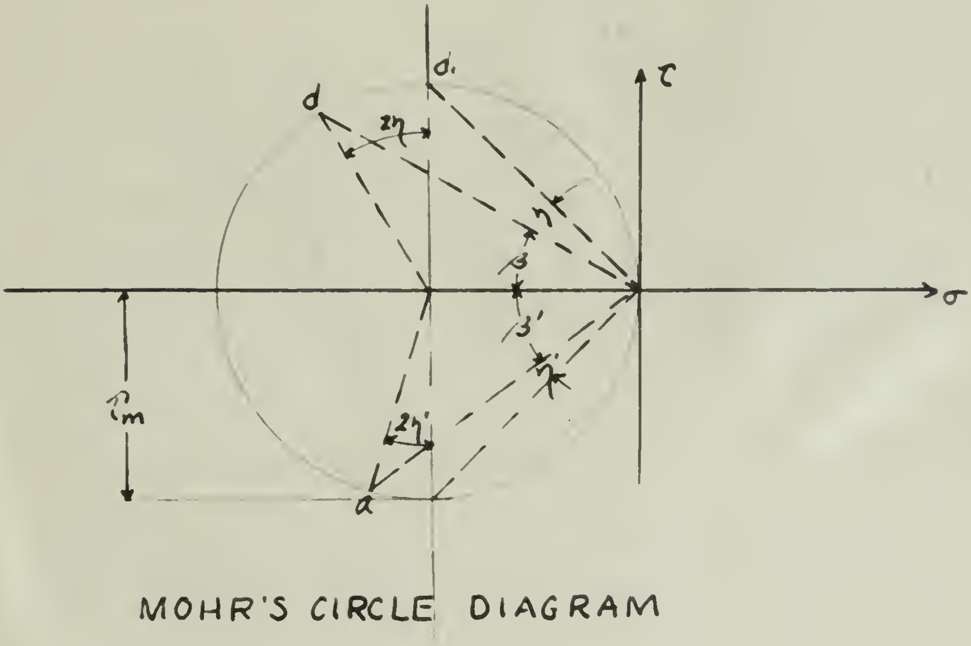
They considered the entire region DBC in fig. 8, including the area between the shear plane and the tool face, to be rigid and in a state of uniform stress. The region could then be represented by a single Mohr's Circle with $\eta'$ representing the angle between the shear plane and the plane of maximum stress, Fig. 9.

The physical justification for $\eta'$ was assumed to be the effective hardness of the chip, which was determined by the shear angle. Their theory states that a decrease in the



STRESS FIELD AT TOOL POINT

figure 8.



MOHR'S CIRCLE DIAGRAM

figure 9.

shear angle increases the constraint on the chip increasing the effective hardness and causing a decrease in $\eta'$

The relations are:

From fig. 8

$$\phi = \eta' - \alpha + \eta \qquad (23)$$

From the Mohr's Circle, fig. 9

$$\eta = 45° - \beta \qquad (24)$$

Therefore

$$\phi = 45° + \eta' - \beta - \alpha \qquad (25)$$

Also from fig. 9

$$\sigma_s = \tau_m (1 + \sin 2\eta') \qquad (26)$$

and

$$\tau_s = \tau_m \cos 2\eta' \qquad \text{or} \qquad (27)$$

$$\frac{\sigma_s}{\tau_s} = \frac{1 + \sin 2\eta'}{\cos 2\eta'} = \tan (45° + \eta') \qquad (28)$$

Their theory of effective hardness was used to justify the relation between $\phi$, $\eta'$ and $\beta$. They showed that a decrease in $\phi$ decreased, not only $\eta'$ as stated, but also $\beta$. Thus

$$\phi = 45° + \eta' - \beta - \alpha \qquad (25)$$

remains in balance and is a true equation.

The exact similarity between Lee and Shaffer's and this solution is noticed. The ratio of normal stress to shear is only slightly differenct in the two solutions.

And once again actual machining data must be relied upon to obtain the value of $\phi$ and $\frac{\sigma_s}{\tau_s}$ to find the final relationship.

Their contribution then is the introduction of another physical theory to explain the phenomenon of metal cutting. Their theory of effective hardness is in accord with the known affects of hardness. However there is no means of testing their theory or measuring "effective hardness" outside of the machining process.

The lack of correlation between separate physical tests and observations and metal cutting analyses and data lead to the conclusion that a full understanding of the metal cutting will result in metal cutting becoming a recognized physical test to indicate physical properties which are not perceivable elsewhere. For there is no other way to distort metal at such enormous strain rates without tremendous and unknown inertia forces, except by metal cutting.

## Thermal Analysis:

In addition to mechanical analysis much has been done on the thermal analysis of metal cutting. It has been found by Epefanov and Rebinder that 99% of the energy consumed by shear in metal cutting is transformed into thermal energy. (37). Friction between tool and chip and tool and workpiece add to

this heat source. With the tool and workpiece strength and hardness directly dependent upon temperature one can see the significance of temperature in metal cutting analysis.

Woxen (8) was the first to attempt a rigid thermal analysis. His assumptions and conclusions have been thoroughly refuted (17). However, it is interesting to note that he reported data in which the interface temperature varied with the square root of the speed. This was later made theoretically predictable (43), although extremely difficult to demonstrate, because of the inconstancy of other variables, such as thermal diffusivity, with temperature. He also predicted that tool life would be some power function of temperature. This was further developed by Schallbroch, Schaumann, and Wallichs (41), and forms part of the practical research of this thesis.

Bisacre and Bisacre were the first to employ rational dimensional analysis to thermal analysis of metal cutting (38). In a study of carbide cutting tools, they introduced to metal cutting thermal analysis the hydrodynamical Reynolds number $\frac{Vd}{\nu}$ in the form of $\frac{Vt}{h^2}$, where v = cutting speed, t = undeformed chip thickness, $h^2$ = thermal diffusivity.

Chao, Trigger and Zylstra (30) then used this parameter, renamed thermal number, in an extensive study of the thermophysical aspects of metal cutting.

Their experimental results indicated that for a given tool rake angle the shear angle, the shear strain, and the temperature rise due to chip shear were all unique functions of the thermal number.

Chao and Trigger (21) further developed the significance of this parameter by reporting experimental data which indicated that the shear angle and specific energy of metal removal can be expressed in terms of the thermal number. Their findings were questioned because they assumed that the thermal diffusivity was constant with temperature, which it is not. However, the degree of error is not determinable because of the lack of precise information concerning the variation of thermal conductivity, and specific heat over the range of temperatures encountered for the materials used.

Hahn (23), Trigger and Chao (17), and Loewen and Shaw (43) have all presented analytical solutions of the problem of temperature determination in metal cutting.

Loewen and Shaw's is the latest. By means of dimensional analysis and Blok's heat transfer technique they derived an expression for the rise in temperature:

$$\bar{\theta}_t - \theta_0 = \frac{\tau}{J}\sqrt{\frac{Vt\gamma}{k_2\rho_2 c_2}}\left[A\mu\sqrt{\frac{t\sec\alpha}{a}} + B\right] \qquad (29)$$

where

$\bar{\theta}_t$ = mean temperature at the interface.

$\theta_0$ = ambient workpiece temperature

$\tau$ = mean shear stress on the shear plane

$J$ = mechanical equivalent of heat

$k_2$ = thermal conductivity of workpiece

$c_2\rho_2$ = volume specific heat of workpiece

$\gamma$ = average shear strain in chip

$\mu$ = coefficient of friction

$a$ = chip contact length

$t$ = chip width

$B$ = a constant

$$\sec\alpha = \frac{r}{\gamma \sin^2\phi}$$

$r$ = ratio of depth of cut to chip thickness

$\phi$ = shear angle

Loewen and Shaw admit the impossibility of verifying this relationship because of the impossibility of holding all other variables constant while investigating the affect of one.

However, it gives a good indication of the variables contributing to high temperatures. Along this line, it predicts the high temperatures met in machining titanium, because of the low thermal diffusivity of the metal.

Empirical Investigations:

Most of the important practical laws concerning metal cutting have been derived from experimental data.

Empirical laws and formulae were developed long before a valid mechanical analysis was made. The major part of the research work in metal cutting now is concerned with finding empirical relationships.

One of the most important subjects of empirical investigations and one of the most important considerations in the economics of metal cutting is a knowledge of the expected tool life. Much theoretical and practical work has been done in attempts to solve the problem of predicting tool life. An interesting indication of the elusiveness of this problem is the number of physical factors which have been investigated in order to find empirical criteria for predicting tool life.

E.J. Janitzky (1) found that tool life could not be related to any one physical test. However, he reported that he found the ratio of Brinell hardness to reduction in area was inversely related to Taylor speed (the cutting speed which dictates a tool life of twenty minutes).

O.W. Boston (2) demonstrated that the varying hardness of one steel, obtained by varying heat treatments was inversely related to Taylor speed. However, he demonstrated, as Janitzky did, that Brinell hardness in itself is no predictable quantitative criterion.

Colwell, Holmes and Roto reiterated that the relationship between Brinell hardness and tool life is poor. However, they reported good correlation between tool life and Knoop hardness, obtained by multiplying the percent of each constituent by its hardness and then dividing the sum of such products by 100.

Ammareller and Koelzer (18) also said that Brinell hardness is no index for evaluating machinability. They found that mechanical properties such as tensile strength, yield stress, elongation and impact toughness provide no unequivocal evidence concerning tool life. They did report an exponential relationship between Taylor speed and shear strength of the chip. They contended that this, coupled with a study of microstructure, is the best basis for predicting tool life.

Lapsley, Grassi, and Thomsen (14), 1950, report that metal cutting data can be correlated with workpiece tensile data and that tensile data offers a useful index to metal cutting. However, they later changed their conclusions (24), 1953, by stating that the correlation of metal cutting with tensile data is doubtful.

If one conclusion can be drawn from these reports I think it is that the microstructure of the material, associated with the hardness of each constituent, is apparently one basic criterion for tool life.

The one classic empirical relation for metal cutting is of course the $VT^n = C$ formula, where $V$ = cutting speed, $T$ = tool life in minutes, $n$ = a constant, in the order of about 1/6, $C$ = a constant, equal to the cutting speed for one minute tool life.

Others relating depth of cut, feed, speed, power, etc., are in every tool handbook.

Less well known is the empirical relation between effective stress and effective strain developed by Lapsley, Grassi, and Thomsen in which

Effective stress $= 110 \times 10^3$ (effective plastic strain) $^{0.152}$

In view of the ideal plasticity assumed for metal cutting, which would indicate an effective Meyer hardenability number of 2, it has been suggested that a rough estimate of plastic strain may be obtained by extrapolating the initial slope of the regular stress strain curve to the ultimate tensile stress. (44 & 39). That is, the plastic strain of metal cutting is sometimes considered roughly equal to ultimate tensile strength/Young's Modulus. This, when introduced into the above formula 30, will give an indication of the effective stress to expect. Although, the above formula is of course intended for the more general use of predicting stress when the actual strain is known.

Another factor which has been related to the problem of metal cutting is that of the deformation energy absorbed by the workpiece during metal cutting (24). It is contended that the work of deforming the workpiece may be a major portion of the total cutting work. The contention is supported by a series of photomicrographs showing surface deformation on the workpiece. It is also contended, that this fact may account in part for the increase in energy per volume of metal removed for extremely small cuts, which is normally attributed to "size effect".

"Size effect" is another factor related to metal cutting. It is observed in other physical phenomena including the increase in tensile strength of specimens of small diameter. It is used to explain the increase in energy per unit volume of metal removed for extremely small cuts. One explanation of this phenomenon is that metals fail because of imperfections which cause stress concentrations. The theoretical statistical probability of there being insufficient imperfections for expected failure in an extremely small cross section is large enough to account for the observed increase in strength.

It would appear that the number of factors which have been related to metal cutting support the contention that metal cutting is such a unique process that fully correlated analyses and criteria can be made only in terms of metal cutting data. The physical factors involved are so numerous as to preclude explanation in terms of any one or two commonly accepted criteria.

# GENERALIZED SOLUTION OF THE SHEAR ANGLE RELATIONSHIP

If it is possible to apply one criticism to all of the analyses of metal cutting, it would be that they all attempt an oversimplification of a very complex problem. Merchant, Lee and Shaffer, and Shaw, Cook and Finnie have all attempted by a single physical phenomenon to explain the variations encountered in metal cutting.

By the imposition of their respective physical concepts they have restricted for their solutions the relationship of the shear angle, friction angle, and rake angle or the relation of the normal stress to the shear stress, $\frac{\sigma}{\tau}$, or both.

For Merchant's original solution that $2\phi + \beta - \alpha = 90$ to be true it is evident from Merchant's diagram, fig. 2, that the normal stress must always be related to the shear stress in such a way that $\frac{\sigma}{\tau} = \cot\phi$ for every material. Considering the large number of variables which affect metal cutting it would be surprising if this restriction applied, which of course it does not.

His next approach, that $\tau = \tau_0 + k\sigma$ permitted more flexibility in the relation of shear angle, friction angle, and rake angle by making them related to some constant C, unique

for each material, that is, $2\phi + \beta - \alpha = C$. However, this assumption that C remained invariant was not correct and imposed a rigidity on his solution leading to lack of correlation between observed and calculated quantities of $\phi$ and $\beta$. Although it afforded more flexibility to the analytical relationship of $\sigma$ to $\tau$, when the expression for the relation $\frac{\sigma}{\tau}$ is written in the form

$$\frac{\sigma}{\tau} = \frac{1}{k}\left(1 - \frac{\tau_0}{\tau}\right)$$

it is evident that the ratio is restricted in that it is a function of the shear stress.

The fact that the actual values of $\sigma$ to $\tau$ are not so restricted is evident from the error of Merchant's derived relationship $2\phi + \beta - \alpha = C$ from the actual value obtained from other measured cutting values.

Lee and Shaffer's adoption of the built-up edge to justify a variable stress field gave a relation of $\sigma$ to $\tau$, that is $\frac{\sigma}{\tau} = 1 + 2\theta$, entirely independent of either variable, and yielded a more flexible solution to the relationship of $\phi$, $\beta$ and $\alpha$. However, in their analysis they imposed the restriction that the maximum shear stress always occurs at the shear plane. This led them to the anomalous position of maintaining that a built-up edge existed where none could be found. It would follow then that the assumption that the shear plane is always

a plane of maximum shear stress is not always true, and that this assumption results in invalid solutions for $\phi$ and $\beta$ at times.

Shaw, Cook and Finnie's theory of effective hardness also yielded a relation of $\sigma$ to $\tau$, $\frac{\sigma}{\tau} = \tan(45^\circ - \lambda')$, completely independent of either variable. However, their assumption that a uniform stress field always exists in the chip after shear in the vicinity of the tool point, restricts the maximum value of normal stress, $\sigma_m$, to that of the maximum value of shear stress, that is, $\sigma_m = \tau_m$. This condition does not necessarily apply at all times, particularly when a built-up edge is present.

It would be reasonable to say that the theories of Lee and Shaffer, and Shaw, Cook and Finnie are complementary rather than contradictory. For a solution to the problem of shear angle relationship can be derived free from the restrictions of both approaches yet incorporating the basic theories of both.

This can be done by adopting Lee and Shaffer's assumption that a variable stress field containing radials of constant shear stress and arcs of varying normal stress exists in the chip after shear for a certain arc [illegible] when a built-up edge exits. However, in deviation from their assumptions, the shear stress on the shear plane is not assumed to be the maximum shear stress value.

This will allow adoption of Shaw, Cook and Finnie's assumption that the shear plane or, when a built-up edge is present, the variable stress region, is at an angle $\eta'$ from the plane of maximum shear stress.

The angles $\theta$ and $\eta'$ are shown in figure 10.

From these two assumptions then the solution can be derived from figure 10 and the corresponding Mohr's Circle diagram in figure 11.

The geometry of figure 10 indicates the angle relationship

$$\phi = \theta + \eta' + \eta + \alpha \tag{30}$$

Mohr's Circle of figure 11 gives

$$\eta = 45^\circ - \beta \tag{31}$$

so that

$$\phi = 45^\circ + \theta + \eta' - \beta + \alpha \tag{32}$$

From figure 11 it is evident that, for the shear plane,

$$\sigma_s = \tau_m (1 + 2\theta + \sin 2\eta') \tag{33}$$

and

$$\tau_s = \tau_m \cos 2\eta' \tag{34}$$

therefore

$$\frac{\sigma}{\tau} = \frac{1 + 2\theta + \sin 2\eta'}{\cos 2\eta'} \tag{35}$$

This relation provides complete independence of $\frac{\sigma}{\tau}$ from $\sigma$ or $\tau$. It also resolves into the solutions of Lee and Shaffer, or Shaw, Cook and Finnie when appropriate.

That is, when $\eta' = 0$ and the shear stress on the shear plane is in fact at the maximum value, the relationships are that

$$\phi = 45^\circ + \theta - \beta + \alpha$$

and

$$\frac{\sigma}{\tau} = 1 + 2\theta$$

as derived by Lee and Shaffer.

When a built-up edge is not present, that is, $\theta = 0$, then

$$\phi = 45 + \eta' - \beta + \alpha$$

and

$$\frac{\sigma}{\tau} = \frac{1 + \sin 2\eta'}{\cos 2\eta'} = \tan(45 + \eta')$$

as derived by Shaw, Cook and Finnie.

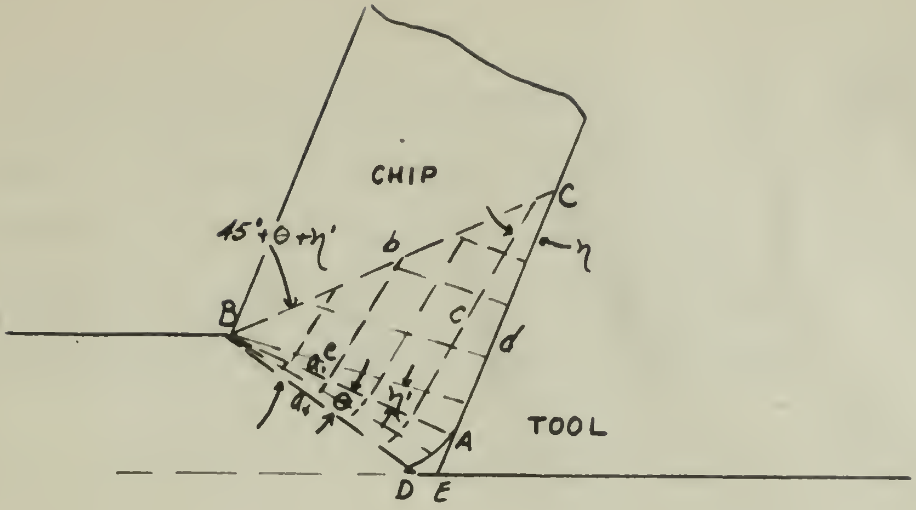
Most important, this solution accommodates the circumstances in between these two extremes, that is, when a built-up edge is present but maximum shear does not occur at the shear plane. Then the general relationships derived that

$$\phi = 45^\circ + \eta' + \theta - \beta + \alpha$$

and

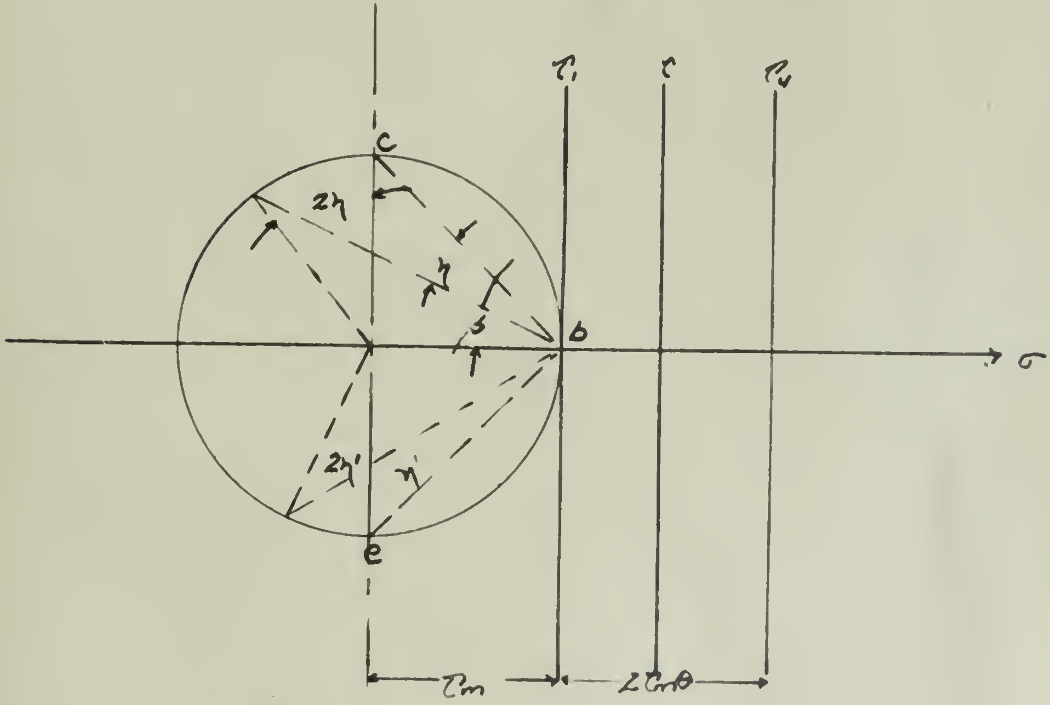
$$\frac{\sigma}{\tau} = \frac{1 + 2\theta + \sin 2\eta'}{\cos 2\eta'}$$

will apply.



STRESS FIELD AT TOOL POINT

figure 10.



MOHR'S CIRCLE DIAGRAM

figure 11.

This solution gives more flexibility and perhaps comes closer to incorporating the actual controlling physical phenomena into the angle and stress relationships of metal cutting than previous analyses. It provides a means for fuller analysis and explanation of metal cutting data.

# PREDICTION OF TOOL LIFE

## Introduction:

"We are still far short of the goal of being able to predict the proper cutting speed for a given tool life for the machining of cast iron, steel, and other materials. Many of the factors which determine tool life are not thoroughly understood, and it appears likely that there are others which have not even been discovered yet. There are so many variables to be considered that the amount of work to be done seems almost limitless."

Colwell, Holmes and Rote 1952 (74)

The ultimate targets of all of the mechanical and thermal analyses and investigations of the machining process are the faster, cheaper and more precise removal of metal.

Any one of these targets is more or less easily attained. The attainment of all three is difficult.

One of the main considerations in the cost of machining is the life of the tools used - measured in time or volume of metal removed before the tool no longer cuts as desired. This may be at complete breakdown or at the loss of dimensional tolerance.

For cheaper machining, second only to the problem of actually prolonging tool life, is the problem of predicting tool life.

Once a tool is formed its actual life is, for any one set of conditions, determined intrinsically. If the production engineer could know what the tool life is, he could plan the manufacturing operation to get the most efficient use of men, machines, and tools.

The complexity of the process of wear and tool failure indicates the size of this problem.

There are generally recognized three types of tool failure. These are temperature failure, rupture failure, and failure from gradual wear.

Temperature failure denotes failure from an excessive temperature which has induced softness from recrystallization, annealing or incipient melting. The tool point fails to cut completely and is rubbed away by the workpiece.

Rupture failure is induced by high forces on the tool, which break off chips from the hard but brittle tool cutting edge. The high forces which cause this failure are usually sharp and intermittent, resulting from chatter or vibration. The forces of a steady cut will not chip the tool usually, regardless of their magnitude. It is noticed that a small amount of chipping may contribute enough thermal energy to the tool edge to cause ultimate temperature failure.

Failure from gradual wear is the type most usually encountered in commercial machining. For single point tools it means a wearing down of the cutting edge on the clearance face of the tool. The surface which remains is called a wearland. Wear may also occur on the top face of the tool back from the cutting edge in the form of a crater.

All of the wear on a tool results from two processes: (1) the welding at points of contact of the tool and workpiece and the consequent immediate rupture of the weld either at the original line of weld, in the workpiece, or in the tool; and (2) the ploughing of extremely hard particles in the workpiece matrix through points on the tool.

The welding process is classified into: (1) temperature welds which occur at temperatures above the recrystallization temperature of the workpiece and (2) pressure welds which occur below recrystallization temperatures.

The temperature welds are usually weaker than either the workpiece or the tool matrix. Consequently, they usually rupture at the original plane of juncture; although enough atoms of tool material are carried away with the chip after rupture to contribute to the wear rate of the tool.

Pressure welds form with less facility than temperature welds because at lower temperature the tool and workpiece are harder and

less likely to adhere. However, when the pressure between the tool and workpiece is great enough to induce the plastic flow in the workpiece necessary to establish the close contact required for the weld, a weld is formed which is harder and stronger than the original workpiece matrix. This results from the strain hardening associated with the pressure weld.

Because of the strength of the weld, rupture usually takes place in the workpiece, leaving the weld deposited on the tool in the form of a built-up edge. As the built-up edge grows it becomes less able to withstand the forces of cutting and pieces of it break off. Some pieces adhere to the chip and effect ploughing wear on the top tool face. Other pieces are imbedded in the workpiece and produce ploughing wear on the tool on the following revolution of the workpiece.

Ploughing wear may also result from extremely hard particles in the original matrix of the workpiece. The alloying elements of vanadium, tungsten, chromium and molybdenum produce extremely hard carbides in steel which induce ploughing. For reducing this type of wear a metallographic structure of a minimum of widely spaced carbide particles is desirable.

A conflict arises, however, for hardness of structure has the desirable affect of inhibiting pressure welding and the wear resulting from it. Therefore, a structure of optimum balance between these two opposing affects is best.

There are other aspects in the relationship of workpiece structure to tool life. Nickel increases the hardness of steel which diminishes pressure welding, however, it also increases the tendency to strain harden which makes the welds and built-up edges which do form much stronger and more damaging. Hardness of workpiece matrix will increase the wear of the tool from temperature welds. A soft matrix will increase the size and frequency of built-up edges, reducing machining accuracy. It is recognized that in every case of machining there is an optimum workpiece crystallographic structure which minimizes the total adverse affects of all of these opposing characteristics.

Although the factors affecting wear are numerous, they are all related, and they are all functions of one variable: temperature. Tool hardness, workpiece hardness and structure, strain hardening, welding are all directly influenced by temperature.

In view of this fact, and in view of the large sources of thermal heat in the cutting process (the plastic deformation at the shear plane, the friction at the tool), it is not surprising that interface temperature between the tool and workpiece is often considered a primary index of tool life (41).

The historical research associated with this thesis indicated that the correlation of tool life with interface temperature for

any one type of tool provided the best avenue for subsequent prediction of tool life for tools of the exact composition and structure. That is, that by the measurement of interface temperature under any cutting conditions one could predict tool life by a knowledge of the tool life associated with that temperature as determined by pilot experiments on tools of the same composition and structure.

Several questions come to mind:

It is commonly agreed that the temperature of a tool determines the hardness, strength and wear resistance of the tool. However, in the cutting operation, does the interface temperature not only determine the characteristics of the tool but also does it indicate the potential wear to which the tool is subjected?

Will a tool subjected to a certain temperature at an arbitrary feed and speed have the same tool life when subjected to the same temperature at a higher feed but compensatingly lower speed?

Will a tool subjected to the same temperature in the cutting of difference workpiece materials have the same tool life?

What affect does tool shape have on the correlation of tool life with temperature?

It is the answers to these questions which have been attempted in the following research.

Method of Test

Equipment Used:

The tools used were Dorklax high speed steel tools containing 18% tungsten, 4% chromium, and 1% vanadium, having a Firth Brown diamond hardness of 946, and Brinell hardness of 712.

The primary workpiece material was hot rolled nickel-chromium-molybdenum steel, S-96, having a Brinell hardness of 279, a Meyer hardenability index of 2.24 as determined on page (d) 2 of the appendix.

The secondary workpiece material was hot rolled nickel-chromium steel DTD 331, having a Brinell hardness number of 402, and a Meyer hardenability index of 2.18. (47).

The lathe used was a Martin lathe manufactured by Boehringer-Sturm-Getriebe D.R.P. and capable of variable speed from 0 to 1330 revolutions per minute. It is depicted on page (c) 1 of the appendix.

Tool vertical forces were measured by means of a College of Aeronautics lathe tool dynamometer depicted on page (c) 3 of the appendix.

The toolmakers microscope depicted on page (c) 4 of the appendix was used to measure the size of wearland.

A Thermocouple as depicted on page (c) 1 of the appendix was used to measure interface temperature. It consisted of a circuit from the insulated tool to a milli-voltmeter, to the workpiece by means of a brush made of a chip of the workpiece material, through the interface between tool and workpiece to the tool.

Test Procedure:

The following test procedure was adopted in order to get values of tool life, interface temperature, and tool force.

Tool life in most cases was the time to complete tool failure except in some instances of excessively long life. In those cases the wearland was measured by the toolmakers microscope and, since the wearland has been shown to vary linearly with time, the measured wearland and corresponding time were extrapolated linearly to obtain a time for .030 inches of wearland, which was considered tool failure.

All test cutting runs were made on the Martin lathe taking advantage of the infinitely variable speed control from 0 - 1330 revolutions per minute to obtain constant speeds.

A constant depth of cut of .100 inches was used with a constant nose radius of 1/32 inches, and a constant clearance angle of 6°.

The cutting edge of the tool was held perpendicular to the workpiece at 0° plan approach angle in all cases.

Test cutting runs at at least four different speeds were made, with measurement of interface temperature, tool force, and tool life, using Dorklax tools on S-96 steel, for each of the following conditions:

(1) Constant .005 inches feed per revolution, variable side rake angle of 5°, 10°, 15°, 20°, 25°, and 30°.

(2) Constant 20° side rake angle, variable feed of .0025, .005, .010, .020 inches per revolution.

Similar cutting runs were then made on DTD 331 steel for the following conditions:

Constant 20° side rake angle, variable feed of .005,&.010 inches per revolution.

All tests were dry.

Results and Discussions:

The results of this investigation are tabulated and shown in appendix (a).

Tool life versus cutting speed curves are shown for each of the conditions investigated.

It is seen from the curve of tool life versus metal removal per tool life on page (a) 5 that, for a given speed, .005 inches feed per revolution consistently yields the highest metal removal before tool failure.

It is also noticed from page (a) 3 that 25° side rake angle gives the best tool life-cutting speed characteristics.

Page (a) 6 shows the curve of interface temperature versus tool life for constant 20° side rake angle at various feeds. The mean lines for each feed coincide, resulting in one tool life interface temperature curve of the form

$$\omega T^{\frac{1}{14}} = 722$$

where $\omega$ = interface temperature in degrees centigrade and T = tool life in minutes. Since four feeds were tested, the consistent coincidence of each curve demonstrates that the decrease in life occasioned by an increase in feed is determined by the corresponding increase in temperature.

The increase in temperature resulting from an increase in feed can be determined from the chart on page (a) 9 where interface temperature versus feed for constant cutting speeds is plotted.

Although the interface temperature versus tool life curves for various feeds coincided, resulting in a single tool life for a given temperature, the metal removal before failure for that tool life varied with the feed. As shown on the chart on page (a) 10, for a given temperature, and consequently given tool life, .020 inches feed per revolution will produce the highest metal removal.

This is explained by examining the charts on pages (a) 8 and (a) 9 of temperature versus cutting speed for constant feeds, and temperature versus feed for constant speeds respectively. The first chart shows that temperature and speed are related so that

$$\omega = CV^{\frac{1}{4.21}}$$

where C is a constant, and V is cutting speed. The second chart shows that temperature and feed are related so that

$$\omega = Kf^{\frac{1}{857}}$$

where K is a constant and f is feed.

It is evident that speed has a more stringent affect on temperature than feed. A determination of the decrease in speed necessary to maintain a constant temperature for an increase in feed will

illustrate this point. If the feed were doubled, say from .010 to .020 inches, the temperature would increase $2^{\frac{1}{8.37}} = 1.084$ times. To counteract this change in temperature to maintain a constant temperature the speed would have to be reduced only to $\left(\frac{1}{1.084}\right)^{4.21} =$ .71 times its former value. Consequently, for a given temperature, the most efficient conditions are at the maximum feed consistent with requirements of power, surface finish and other considerations.

The chart on page (a) 11 shows interface temperature versus tool life for three side rake angles. For a given temperature, tool life decreases with increasing rake angle.

The interface temperature versus cutting speed chart shows that 25° side rake angle accommodates the highest speed for a given temperature.

It is interesting to note from the chart on page (a) 13 the small variance in metal removal per tool life for a given temperature for the various side rake angles, in spite of the divergence in the speeds for a given temperature. 25° side rake angle shows slightly greater metal removal for a given temperature.

The results of the investigation of DTD 331 show that within the accuracy of temperature measurement the interface temperature versus tool life curve coincides with that for S-96 (chart Page (a) 15).

There is of course divergence in the cutting speed and metal removal per tool life for DTD 331 and S-96 as shown on the charts on pages (a) 16 and (a) 17. DTD 331 shows lower speeds and lower metal removal for a given temperature.

Chart (a) 18 shows how interface temperature and metal removal per tool life vary for constant speeds. For each speed there exists a temperature for maximum metal removal (604° C for 80 fpm).

The interface temperature versus metal removal per tool life for constant feeds of .010 and .020 inches is shown on page (a) 19 indicating temperatures for maximum metal removal of 545° and 555° C respectively.

Conclusions:

The following conclusions appear justified:

For a HSS tool cutting high strength steels the interface temperature is a true index of tool life.

It is apparent from this that the interface temperature determines both the strength of a tool and the potential wear to which it is subjected.

For a given temperature there is an optimum feed and speed for the maximum metal removal. Because of the nature of the temperature feed relationship $\omega = Kf^{\frac{1}{357}}$ and temperature speed relationship $\omega = CV^{\frac{1}{4.21}}$, the optimum condition is usually at the maximum feed possible. This does not apply at very low speeds where these relationships vary because of the excessive forces encountered.

For a given feed there is a temperature for maximum metal removal per tool life. This temperature is not the same for every feed although it is a consistently low temperature corresponding to the lowest practical speed before excessive forces incur deviation from the usual temperature tool life relationship.

For a given speed there is an optimum temperature for maximum metal removal. The optimum temperature is not the same for each speed. For higher speeds the optimum temperature is higher.

The temperature tool life relationship for a given tool material varies with the tool shape. For a given temperature the tool life decreases with increasing rake angle. There is, however, a side rake angle inducing a maximum metal removal for a given temperature, in this case 25°.

Although the experimental results show that tool life for a given tool and a certain workpiece could be predicted from measuring interface temperature and noting the corresponding tool life from data determined on another workpiece material, the nature of the tool life interface temperature relationship probably precludes the practical adoption of this procedure. In this case tool life was inversely related to the fourteenth power of interface temperature. An error of two percent in temperature measurement would result in an error of 32% in tool life prediction.

This point is illustrated by these experimental results. For DTD 331, the temperature and tool life values determined the same curve as that for S-96. Yet if the measured temperature values had been used to determine tool life from the S-96 curve, errors of from 10 to 30 percent would have resulted.

In some cases this order of accuracy may be satisfactory. In all cases it should be recognized and considered.

## MILLING CHARACTERISTICS OF DTD 331

### Introduction:

One of the major problems of modern aircraft production is the efficient machining of the high strength steels necessary for high performance aircraft.

In order to maintain production efficiency the production engineer must know with reasonable accuracy the cutting characteristics and the optimum tool life conditions for the steel he is going to use.

An investigation was undertaken for this purpose of determining the end milling characteristics and optimum end milling tool life conditions of DTD 331 steel using a one inch diameter end mill.

When the time and material are available, tests similar to this can be made for every new steel which is introduced. However, this procedure is costly. There is a recognized need or desire for some means of using the results of say some simple turning tests to determine other machining characteristics such as milling.

In order to test the feasibility of some direct measurable criterion for relating milling and turning tool life data for

DTD 331, turning tool life tests were made with high speed steel tools of the same composition as the milling cutter, as part of this investigation, for comparison with the tool life data from the milling tests.

## Method of Test

### Equipment Used:

The milling cutters were one inch diameter, five-toothed end mills of steel containing 6% tungsten, 5% molybden and 2% vanadium, having a helix angle of 30° and a radial rake angle of 15°, and having a Firth Brown diamond hardness of 716, and a Brinell hardness of 600.

Special Dormer tool bits of the same composition having a Firth Brown diamond hardness of 824 and Brinell hardness of 675 were used for the turning tests.

The workpiece material was nickel-chromium steel DTD 331 described previously.

The lathe used was the Martin lathe used in the previous tests.

The milling machine was a Loewe milling machine depicted on page (c)2 of the appendix.

The toolmakers microscope was again used to measure wear.

### Test Procedure:

Straight life tests were run on bars of DTD 331 using the one inch end mills at a constant .050 inch depth of cut and a constant .1 inch width of cut.

Life tests at several difference peripheral speeds were made for the following advance rates per tooth: .003, .004, .006, .008, .011, .016 inches or maximum chip thickness of .001, .0013, .002, .0028, .0036, .005 inches respectively.

Life values were determined in all cases by measuring with the toolmakers microscope the wear of each tooth, determining an average wear, and adjusting the measured time linearly to a time for an average wearland of .024 inches.

Because of the work hardening characteristics of DTD 331 it was found that usually uneven wear occurred. The part of the cutter cutting a work hardened surface (about .004 inches wide usually) wore much faster than the rest of the width of cut. The maximum wear incurred on any part of the tooth regardless of the shape of the remainder of the wearland was the measured value in all cases.

The turning tests were made on the Martin lathe using the Dormer tools with constant 1/32 inch nose radius, constant 6° clearance angle, constant 15° side rake angle, and constant 0° plan approach angle.

Life tests were made at four speeds at each of .0025, .005, and .010 inches feed per revolution.

Tool life was determined as time to failure or as the time, adjusted linearly, for .024 inches wear.

All tests were dry.

Results and Discussion:

The results are tabulated and shown in appendix b.

In all cases for milling, tool life is defined as the equivalent continuous cutting time per tooth. Cutting speed in all cases is the peripheral speed of the cutter in feet per minute.

The cutting speed tool life curves for constant maximum chip thicknesses are shown on pageb1 of the appendix. It is evident that tool life falls off for a decrease in maximum chip thickness below .0013 inches or decrease in advance per tooth below .004 inches.

The real nature of the cutting speed, maximum chip thickness, and table advance relationships are brought out on pageb2 where tool life in minutes is plotted against metal removal per tool life per tooth. Lines of constant speed, constant maximum chip thickness, and constant table advance per minute are shown.

It is evident that, for a constant speed, maximum tool life is obtained in all cases at a maximum chip thickness of .0013 inches. An increase or decrease in maximum chip thickness results in decreased tool life.

It is shown clearly for every speed that maximum metal removal per tool life occurs at 14.75 inches per minute table advance.

This fact is not indicated in the normal tool life cutting speed curves. While one would expect that for every cutter

rotating speed there is an optimum advance rate for maximum metal removal per tool life, it would not necessarily be expected that this would occur at one advance rate for every cutter speed. One would have expected, more likely, an optimum chip thickness or advance per tooth.

The obviousness of the actual characteristics of the milling data on this type of plot would seem to justify its adoption as the best means of showing tool life data.

The tool life cutting speed curves for turning are shown on page63.

In order to find a correlation between turning data and milling data comparative plots of equal feed per revolution for turning and maximum chip thickness for milling were plotted for tool life versus cutting speed (page64). There is no simple direct relation between the two.

A similar plot of metal removal per tool life versus cutting speed for equal chip thickness and speed was made for comparison. Again the milling curve is so far removed from the turning curve that simple direct comparison of data is impossible.

Tool life versus cutting speed was plotted, page67, for constant feed per revolution for turning and equal advance per tooth for milling. Again there is no simple direct relationship.

This lack of simple direct relationship between tool life for turning and tool life for milling for the same speed and feed or maximum chip thickness is not surprising when one considers the different wear processes.

The turning tool must withstand a constant high temperature at the interface. The pressure of the chip practically excludes all oxygen, so there is, effectively, no chance for a protective oxide film to form. Continuous clean bare metal of the chip is rubbed against the clean tool. All of these conditions contribute to high wear rates.

The milling cutting is not subjected to a continuous high temperature. An individual tooth is subjected to cutting forces and temperature for only about .05 of the time for a revolution. Although tool life is determined as the equivalent continuous cutting time, the fact that the tool is cutting only a very short time continuously means that it will be at a much lower temperature than equivalent feed and speed for turning. In addition to cooling the cutter, its exposure to the air affords the formation of a protective layer of oxide.

On the other hand, the intermittent nature of a milling cut induces high wear particularly on a steel of the strength and hardness of DTD 331.

It would be coincidental if these different wear processes result in equal tool lives for milling and turning for similar conditions.

It was found, however, that there was a consistent relationship between the tool lives for milling and cutting for equal feed and speed.

It was found that the cutting speeds for one minute tool lives were related in such a way that $C_t f^y = C_m$, where $C_t$ = cutting speed for one minute tool life for turning, $f$ = feed, $y$ = constant exponent, and $C_m$ = cutting speed for one minute tool life for milling. From the plot of tool life versus cutting speed for .0025 inches feed (or maximum chip thickness), page64, it was found that $(.0025)^y = \frac{84}{130}$ or that $y = \frac{1}{13.6}$.

It was also found that the slopes of the tool life versus cutting speed curves for milling and turning were related so that $n_m = n_t f^{-x}$ where $n_m$ = slope for millint, $n_t$ = slope for turning, $f$ = feed, and $x$ = constant exponent. Once again from page64

$$\frac{1}{3.6} = \frac{1}{8}(.0025)^{-x} \text{ or } x = \frac{1}{7.5}$$

From this one plot of tool life versus cutting speed the constants could be found for determining the milling curve from a known turning curve by the combined formula of the form

$$V_m T_m^{n_t f^{-x}} = C_t f^y$$

which in this case was

$$V_M T_M^{n_T f^{-\frac{1}{7.5}}} = C_T f^{\frac{1}{136}}$$

In order to check the formula, the tool life cutting speed curve for turning for .005 inches feed was plotted. The slope was measured at $n_t = \frac{1}{8}$. The cutting speed for one minute tool life equals 100 fpm. Substituting these in the formula gives

$$V_M T_M^{\frac{1}{8}(.005)^{-\frac{1}{7.5}}} = 100(.005)^{\frac{1}{136}}$$

or

$$V_M T_M^{\frac{1}{3.94}} = 67.7$$

This was plotted as shown on page 64. The measured line for .005 inches maximum chip thickness was then plotted for comparison. The difference between the measured and predicted lines is negligible.

## Conclusions:

The plot of tool life versus metal removal per tool life per tooth is evidently the best means of exhibiting milling characteristics. The best conditions for any criteria is immediately evident. If maximum tool life is desired for any speed the appropriate chip thickness and table advance is easily determined. Similarly, conditions for maximum metal removal are evident.

The data of these tests indicate that their is no simple direct relationship between milling and turning cutting speed tool life characteristics for equal feed and maximum chip thickness.

However, if for a new material one milling test is run for one maximum chip thickness and a turning test is run for the same feed, the values of x and y in the following formula can be determined. They are constants for the work material and tool. Thereafter the milling cutting speed tool life curve can be determined from any given turning curve by applying the formula

$$V_m T_m^{n_t f^{-x}} = C_t f^{y}$$

In this case, for DTD 331 and the tools used,

[illegible]

# INVESTIGATION INTO A GRAPHICAL TECHNIQUE OF DETERMINING NO-WEAR TOOL FORCES

## Introduction:

The paradox in metal cutting is that it is predominantly a shearing process yet it does not lend itself to analysis and explanation in terms of the known shear strength tests.

The forces involved are much higher than predicted by normal stress strain relationships.

There have been three main explanations for this paradox.

The reasoning of Merchant and Shaw is that static shear tests with normal stresses imposed of magnitudes similar to those of metal cutting and corrected for the effect of strain hardening would predict and ccount for the stresses and strains found in metal cutting.

This explanation is opposed by Colwell, Holmes, and Rote and others who contend that known shear tests with normal stresses imposed corrected for strain hardening do not account for the shear stress in metal cutting. They, and Merchant and Shaw admit, that it is impossible to duplicate the conditions of metal cutting by any other means than metal cutting. Their conclusion is that the high stresses and high strain rates of metal cutting in the

absence of inertia forces justify adopting metal cutting as a physical test and criterion in itself.

A third approach has been that the measured cutting forces have been erroneously high and that when they are reduced by graphical extrapolation to nullify these errors, better correlation with known data is obtained.

One of these methods was put forward by Thomsen, Lapsley and Grassi (31). They contended that deformation of the workpiece contributed substantially to the forces of cutting. They found that for a constant speed and feed at small depths of cut the cutting forces caried linearly with the depth of cut. By plotting cutting forces versus depth of cut, at depth of cut up to .004 inches, and extrapolating the force depth curves linearly to zero depth of cut they obtained a zero depth of cut force. They contended that the zero depth of cut force is an approximate estimate of the force required to deform the workpiece and is not available for chip deformation. They concluded that it should be deducted from the measured force when analyzing the chip shear.

Another method was put forward by Goddard who contended that "it is not possible to obtain force readings in less than ten seconds" and that by that time appreciable tool wear has taken place which would increase the measured forces. He found that by plotting force versus wear and extrapolating to zero wear a

lower expression for force was obtained which was put forward as a true value of the actual force for cutting.

Since both of these methods result in lower forces for the cutting process, better correlation between analyses and static physical data is obtained. This in itself, however, is not justification for the adoption of the methods.

It is recognized that when analyzing the cutting process for a tool of a given shape the influence of tool wear on measured forces must be excluded. An investigation was made therefore into the justification and necessity of the proposed graphical method of finding the force for no wear.

Method of Test:

Tests were conducted on the Martin lathe using S-58 carbide tipped tools in the lathe dynamometer on DTD 331 and S-96.

Five cutting tests on S-96 were made. Two were at .005 inches feed per revolution at 200 and 240 feet per minute cutting speed. Three were at .010 inches feed at 200, 240, and 300 feet per minute cutting speed.

Three cutting tests were run on DTD 331 at a feed of .005 inches per revolution and speeds of 200, 240 and 270 feet per minute.

In each case force readings were taken immediately, within three seconds, and the tool was withdrawn and measured.

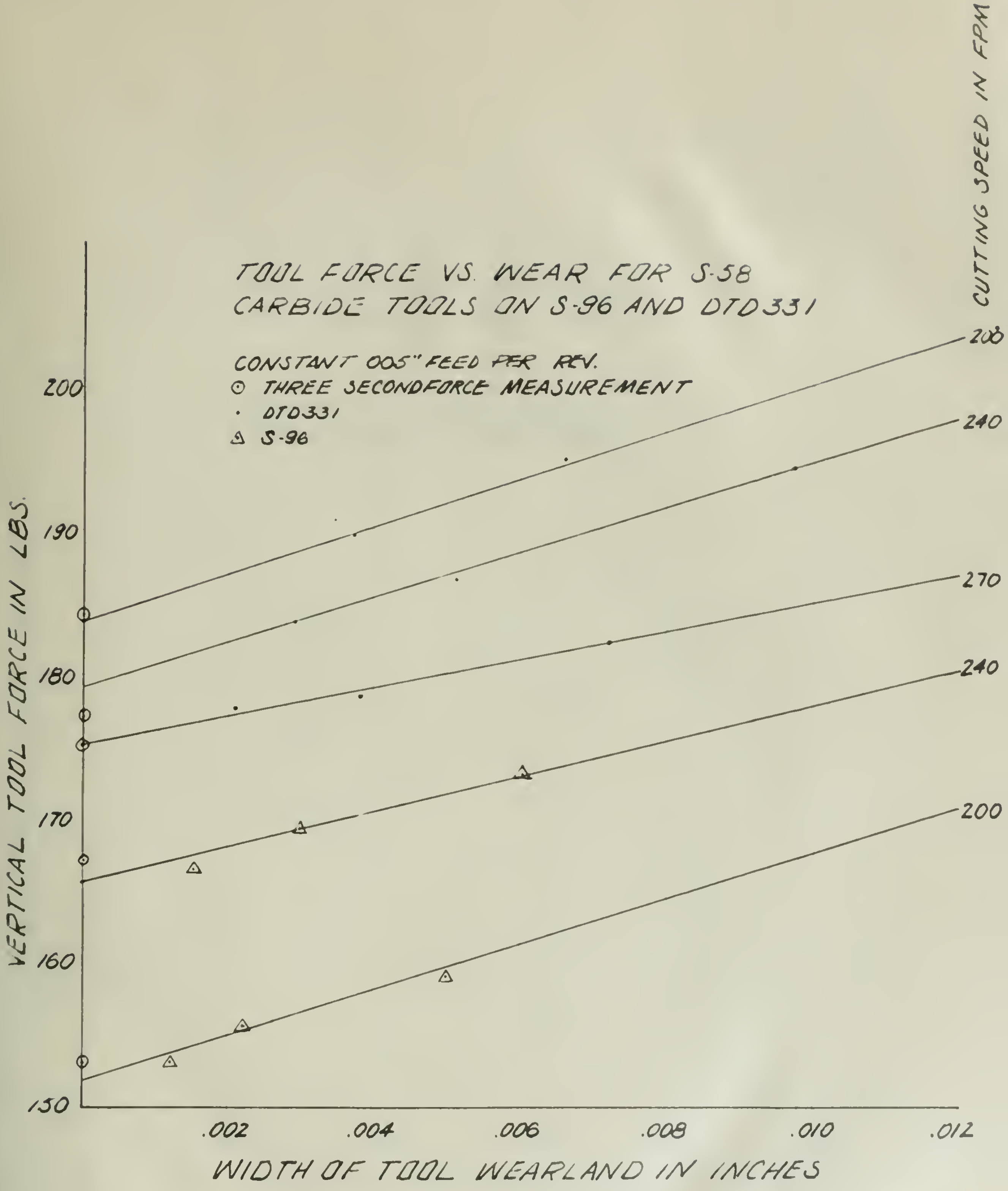
The tool was re-inserted and the cut was continued for a total of 60 seconds at which time force measurements were recorded and tool wear was measured.

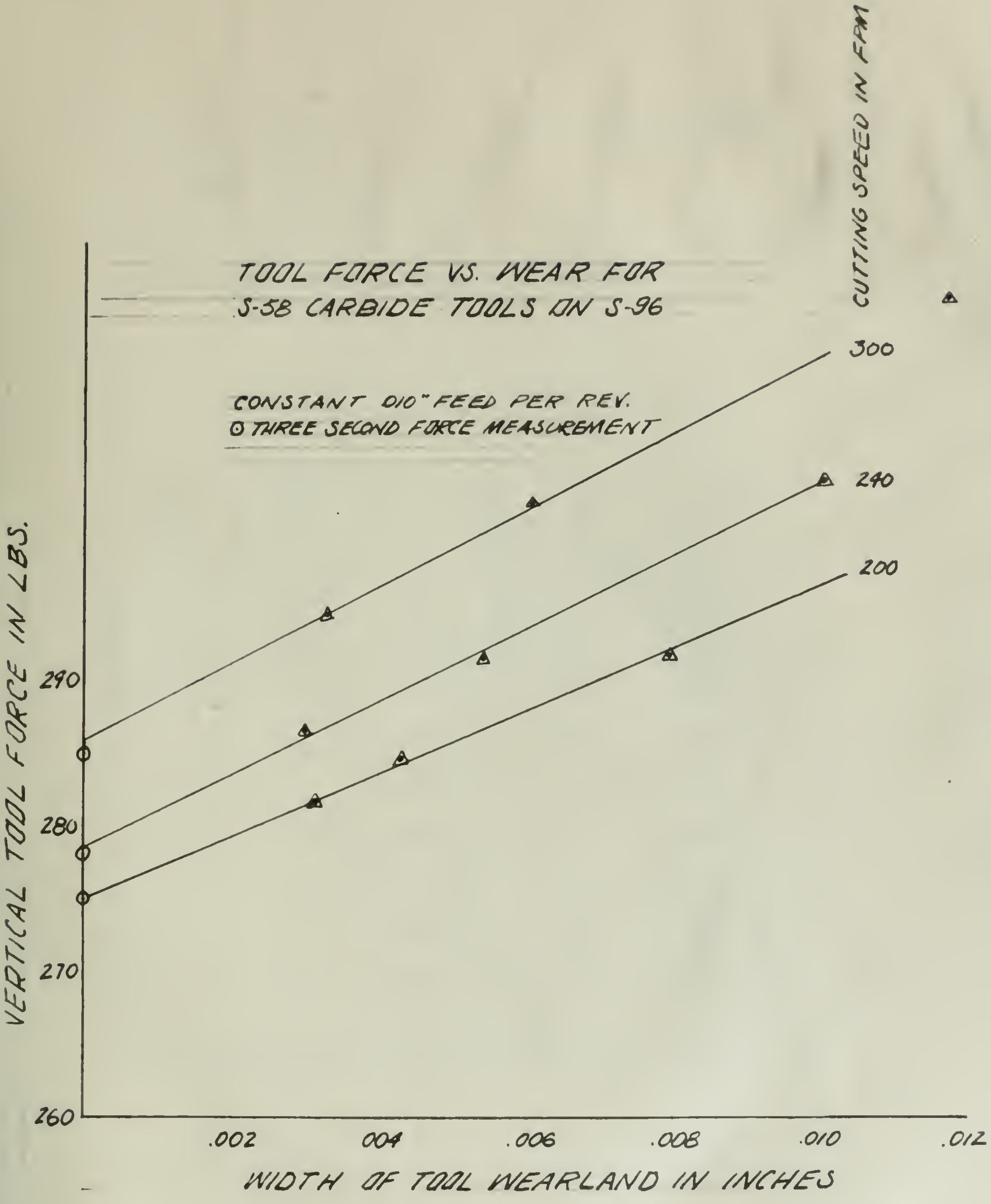
The tool was re-inserted and the cut continued for an additional three minutes, or total of four minutes, at which time force and wear measurement were again taken.

The tool was re-inserted and the cut continued for a total of ten minutes at which time force and wear measurements were again recorded.

Results:

The results are shown on the charts on the following two pages.


TOOL FORCE VS. WEAR FOR S-58 CARBIDE TOOLS ON S-96 AND DTD331
CONSTANT .005" FEED PER REV.
⊙ THREE SECONDFORCE MEASUREMENT
· DTD331
△ S-96
CUTTING SPEED IN FPM
200
240
270
240
200
VERTICAL TOOL FORCE IN LBS.
150
160
170
180
190
200
.002
.004
.006
.008
.010
.012
WIDTH OF TOOL WEARLAND IN INCHES


TOOL FORCE VS. WEAR FOR
S-58 CARBIDE TOOLS ON S-96
CONSTANT .010" FEED PER REV.
⊙ THREE SECOND FORCE MEASUREMENT
CUTTING SPEED IN FPM
300
240
200
VERTICAL TOOL FORCE IN LBS.
290
280
270
260
.002
.004
.006
.008
.010
.012
WIDTH OF TOOL WEARLAND IN INCHES

## INVESTIGATION INTO A GRAPHICAL TECHNIQUE OF DETERMINING NO WEAR FORCES

DATA:

(For S-58 carbide tools cutting S-96).

| SIDE RAKE ANGLE | FEED IN/REV | CUTTING SPEED FPM | CUTTING TIME MIN-SEC | CUTTING FORCE SCALE RDG. | CUTTING FORCE LBS. | WIDTH OF WEARLAND (IN.) |
|---|---|---|---|---|---|---|
| 15° | .005 | 200 | 0-03 | 18.4 | 153 | .000 |
| | | | 1-00 | 18.4 | 153 | .0012 |
| | | | 4-00 | 18.7 | 155.5 | .0022 |
| | | | 10-00 | 19.1 | 159 | .0050 |
| | | 240 | 0.03 | 20.1 | 167 | .000 |
| | | | 1-00 | 20.1 | 167 | .0015 |
| | | | 4-00 | 20.3 | 169.5 | .0030 |
| | | | 10-00 | 20.8 | 173 | .0060 |

INVESTIGATION INTO A GRAPHICAL TECHNIQUE OF

DETERMINING NO WEAR FORCES

DATA:

(For S-58 carbide tools cutting S-96)

| SIDE RAKE ANGLE | FEED IN/REV | CUTTING SPEED FPM | CUTTING TIME MIN-SEC. | CUTTING FORCE SCALE RDG. | CUTTING FORCE (LBS.) | WIDTH OF WEARLAND (IN.) |
|---|---|---|---|---|---|---|
| 15° | .010 | 200 | 0-03 | 33.0 | 275 | .000 |
| | | | 1-00 | 33.8 | 281.5 | .0031 |
| | | | 4-00 | 34.2 | 284.5 | .0042 |
| | | | 10-00 | 35.0 | 291.5 | .0090 |
| | | 240 | 0-03 | 33.4 | 278 | .000 |
| | | | 1-00 | 34.4 | 286.5 | .0029 |
| | | | 4-00 | 35.1 | 292.5 | .0054 |
| | | | 10-00 | 36.4 | 303.5 | .0100 |
| | | 300 | 0-03 | 34.3 | 285 | .000 |
| | | | 1-00 | 35.4 | 294.5 | .0033 |
| | | | 4-00 | 36.2 | 302 | .0060 |
| | | | 10-00 | 37.9 | 316 | .0117 |

INVESTIGATION INTO A GRAPHICAL TECHNIQUE OF DETERMINING NO WEAR FORCES

DATA:

(For S-58 carbide tools cutting DTD 331).

| SIDE RAKE ANGLE | FEED IN/REV | CUTTING SPEED FPM. | CUTTING TIME MIN-SEC | CUTTING FORCE SCALE RDG. | CUTTING FORCE LBS. | WIDTH OF WEARLAND IN. |
|---|---|---|---|---|---|---|
| 15° | .005 | 200 | 0-03 | 22.1 | 184 | .000 |
| | | | 1-00 | 22.7 | 189.5 | .0037 |
| | | | 4-00 | 23.4 | 195 | .0066 |
| | | | 10-00 | 24.5 | 204 | .0125 |
| | | 240 | 0-03 | 21.2 | 177 | .000 |
| | | | 1-00 | 22.0 | 183.5 | .0029 |
| | | | 4-00 | 22.4 | 186.5 | .0051 |
| | | | 10-00 | 23.3 | 194 | .0098 |
| | | 270 | 0-03 | 21.0 | 175 | .000 |
| | | | 1-00 | 21.3 | 177.5 | .0021 |
| | | | 4-00 | 21.4 | 178.5 | .0038 |
| | | | 10-00 | 21.8 | 182 | .0072 |

In every case the force was noted and the tool was withdrawn from the cut within three seconds. There was no measurable wear at three seconds in any case.

The charts on the following two pages show the original force measurement encircled on the axis of zero wear. It is noticed that the original measured value of force and the value of force for no wear, obtained by extrapolating the line determined by the other three points, coincide well within the accuracy of the force readings.

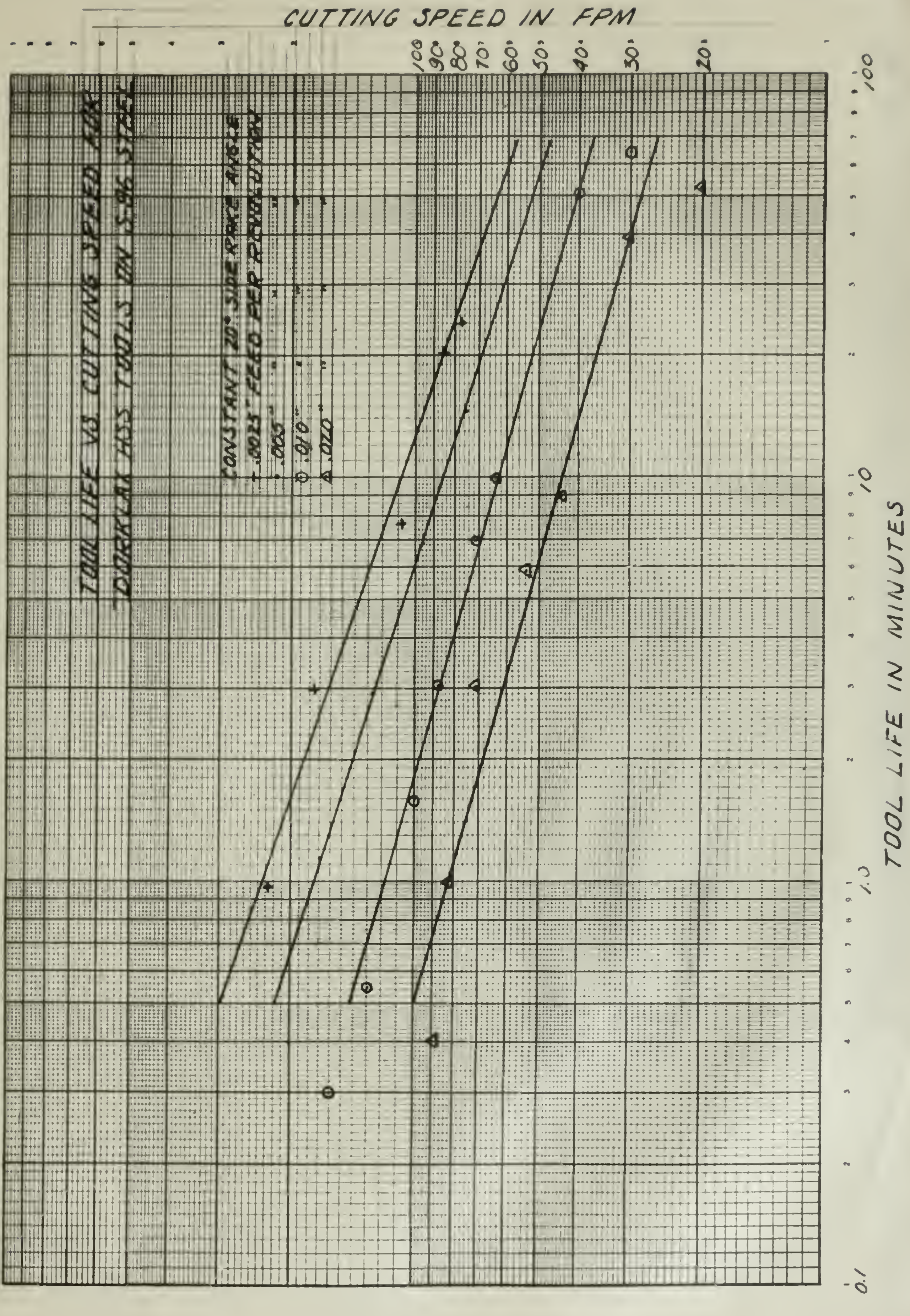
Conclusions:

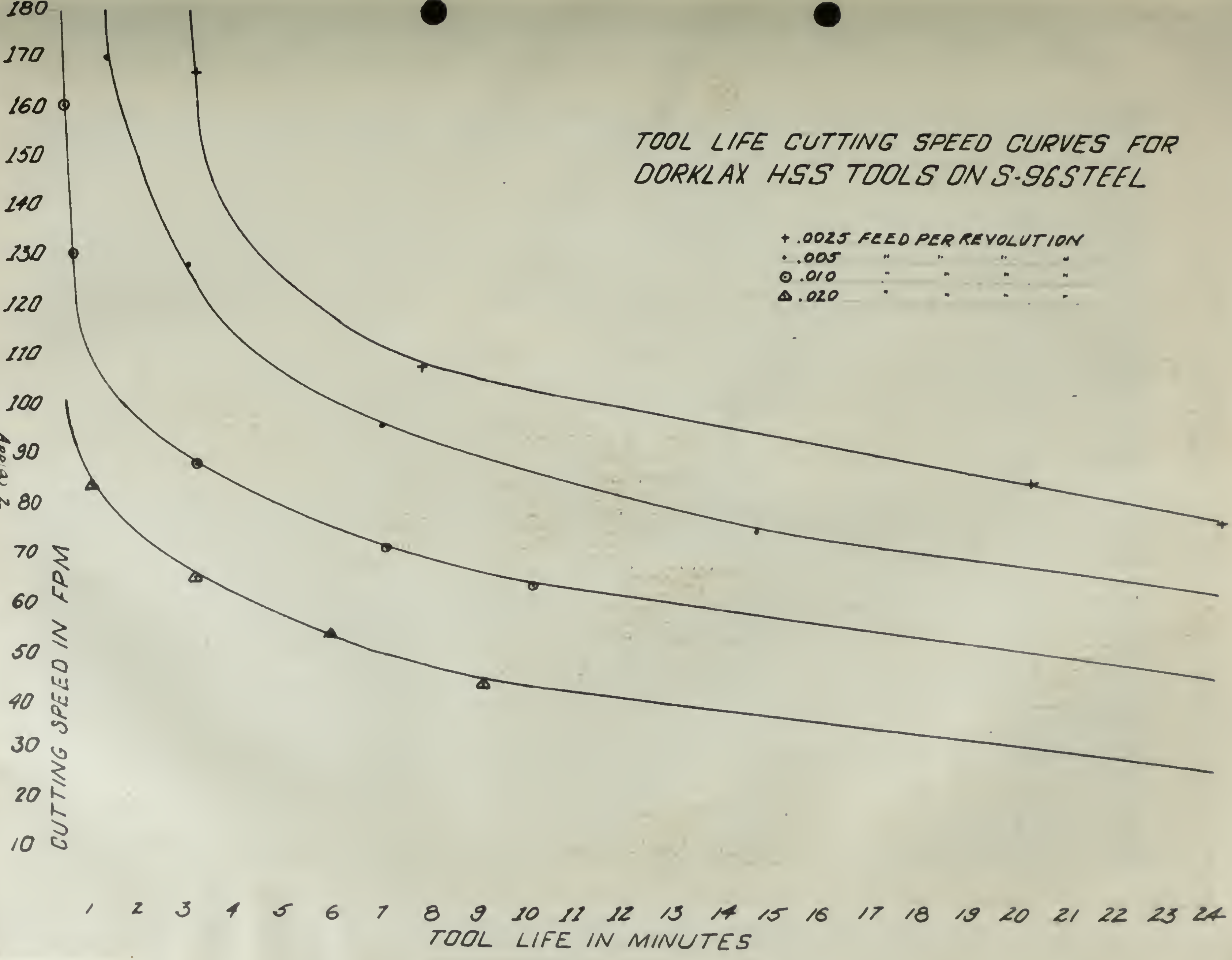
The limitation that the force reading could not be taken for at least ten seconds was not found to apply for these cuts.

It was found that force readings could be taken within three seconds before wear measurable by microscope or force increase had taken place.

The coincidence of extrapolated and measured forces at no wear indicate the validity of obtaining the no wear force by extrapolation if it is impossible to measure the force before finite wear occurs.

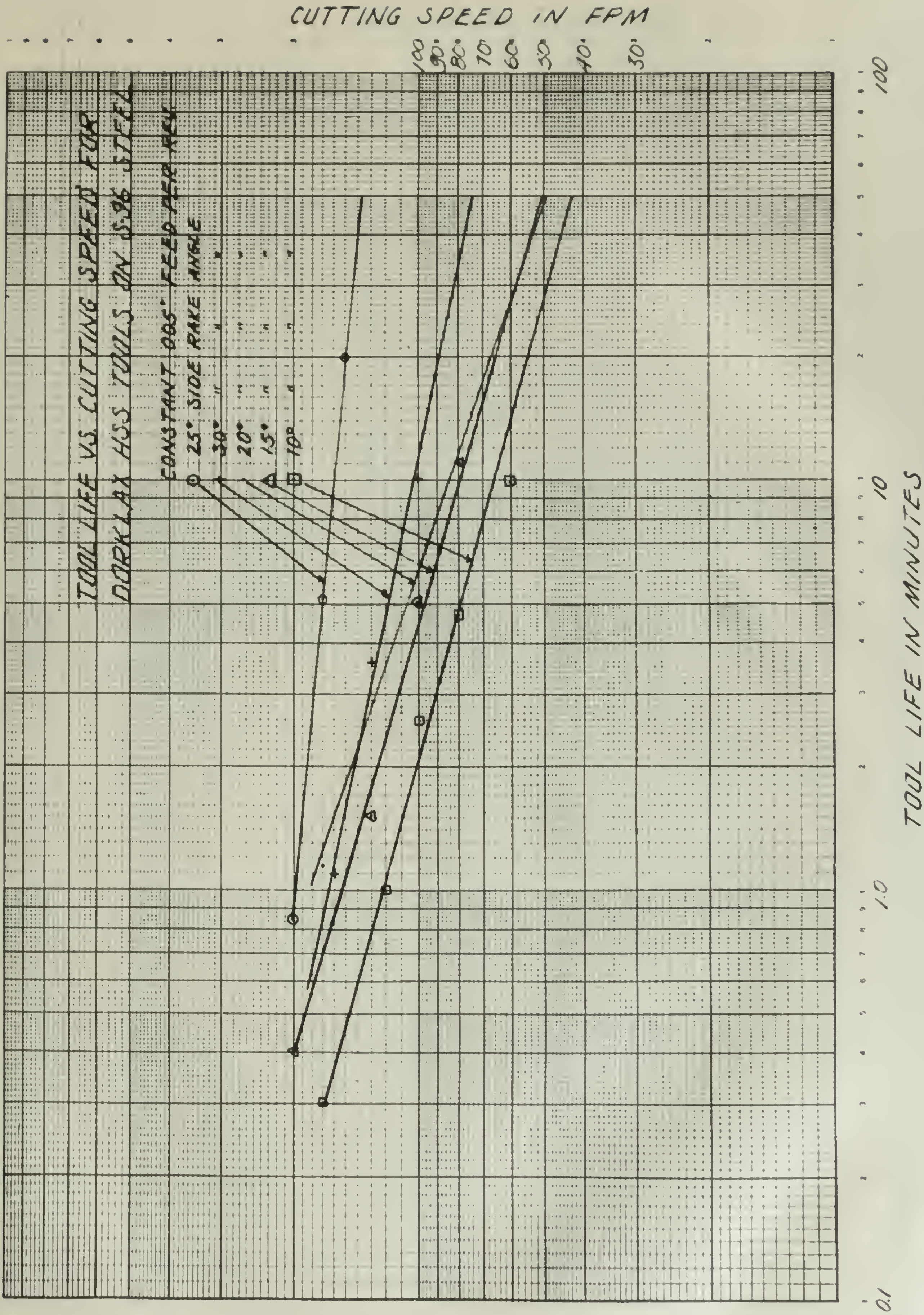
However, there is no apparent necessity for relying on a graphical means of extrapolation to determine a quantity which can be measured directly.


TOOL LIFE VS CUTTING SPEED FOR
DORKLAX HSS TOOLS ON S.96 STEEL
CONSTANT 20° SIDE RAKE ANGLE
+ .0025" FEED PER REVOLUTION
• .005" " " "
⊙ .010" " " "
△ .020" " " "
CUTTING SPEED IN FPM
100
90
80
70
60
50
40
30
20
TOOL LIFE IN MINUTES
0.1
1.0
10
100

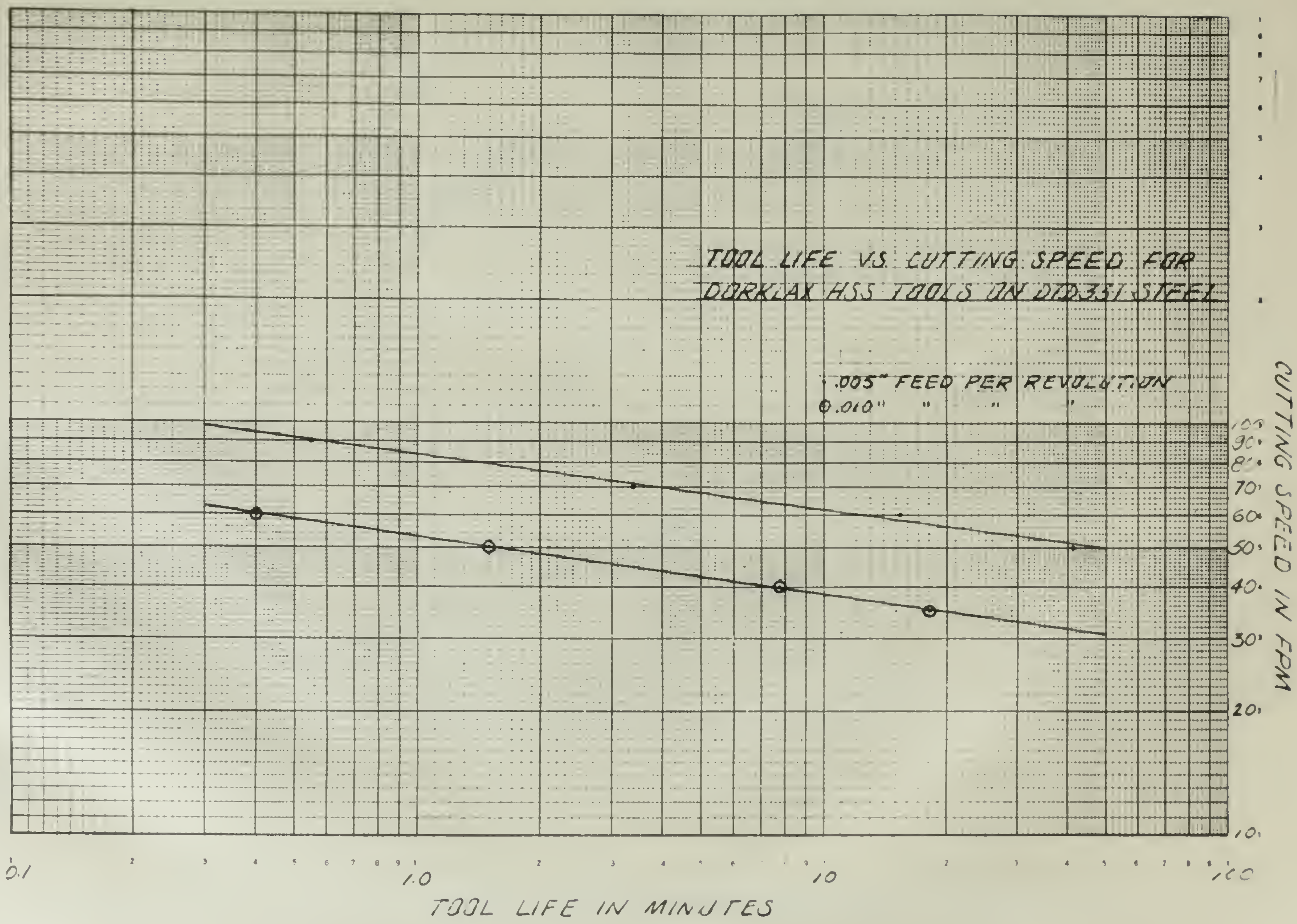


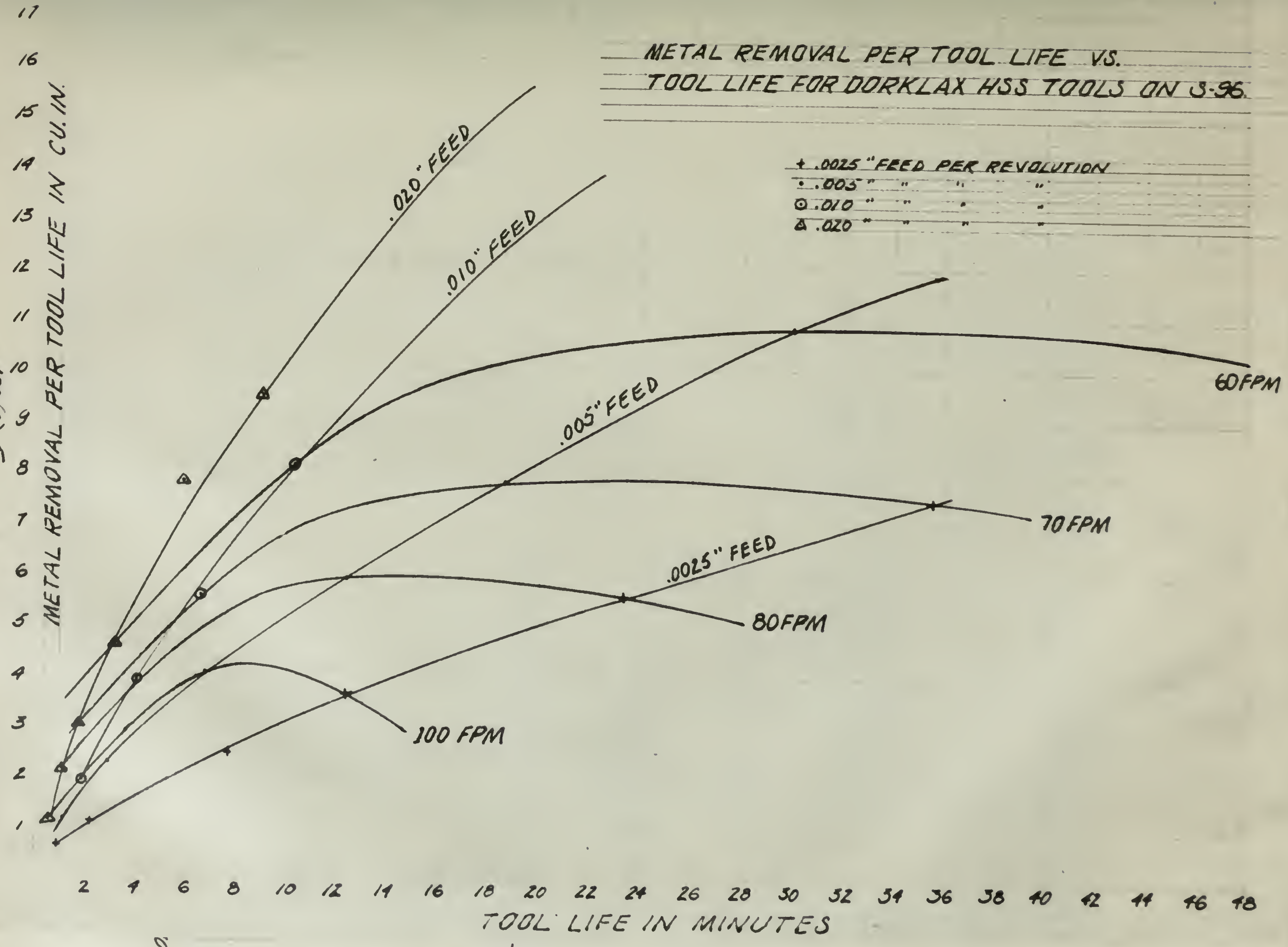
TOOL LIFE CUTTING SPEED CURVES FOR DORKLAX HSS TOOLS ON S-96STEEL

+ .0025 FEED PER REVOLUTION
• .005 " " "
⊙ .010 " " "
△ .020 " " "

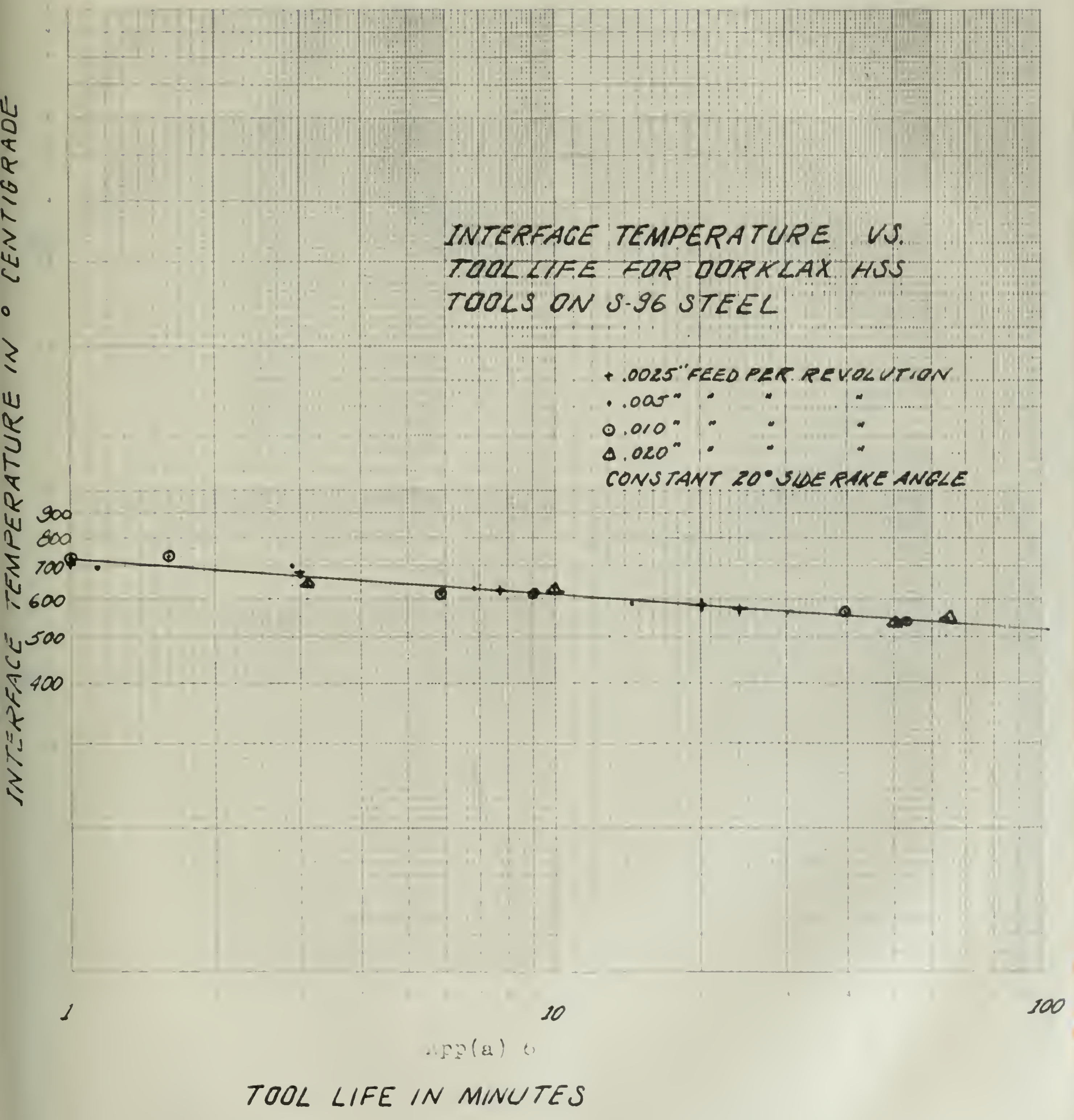


CUTTING SPEED IN FPM
100
90
80
70
60
50
40
30
TOOL LIFE VS. CUTTING SPEED FOR
DORKLAX HSS TOOLS ON S.96 STEEL
CONSTANT .005" FEED PER REV
25° SIDE RAKE ANGLE
30° " " "
20° " " "
15° " " "
10° " " "
TOOL LIFE IN MINUTES
0.1
1.0
10
100

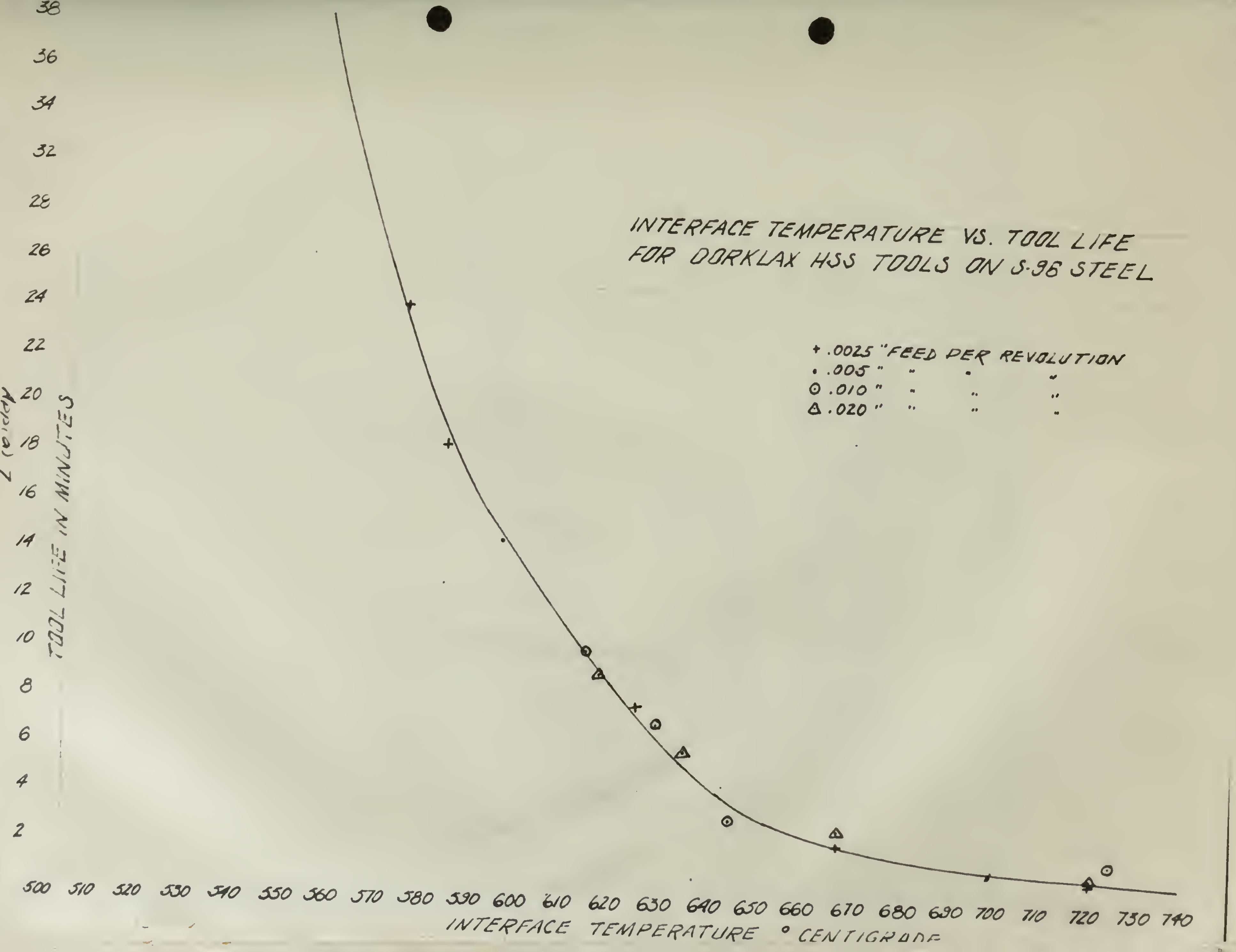

TOOL LIFE VS. CUTTING SPEED FOR
DORKLAX HSS TOOLS ON DTD.351 STEEL
·.005" FEED PER REVOLUTION
⊙.010" " " "
CUTTING SPEED IN FPM
100
90
80
70
60
50
40
30
20
10
TOOL LIFE IN MINUTES
0.1
1.0
10
100



METAL REMOVAL PER TOOL LIFE VS.
TOOL LIFE FOR DORKLAX HSS TOOLS ON S-96.

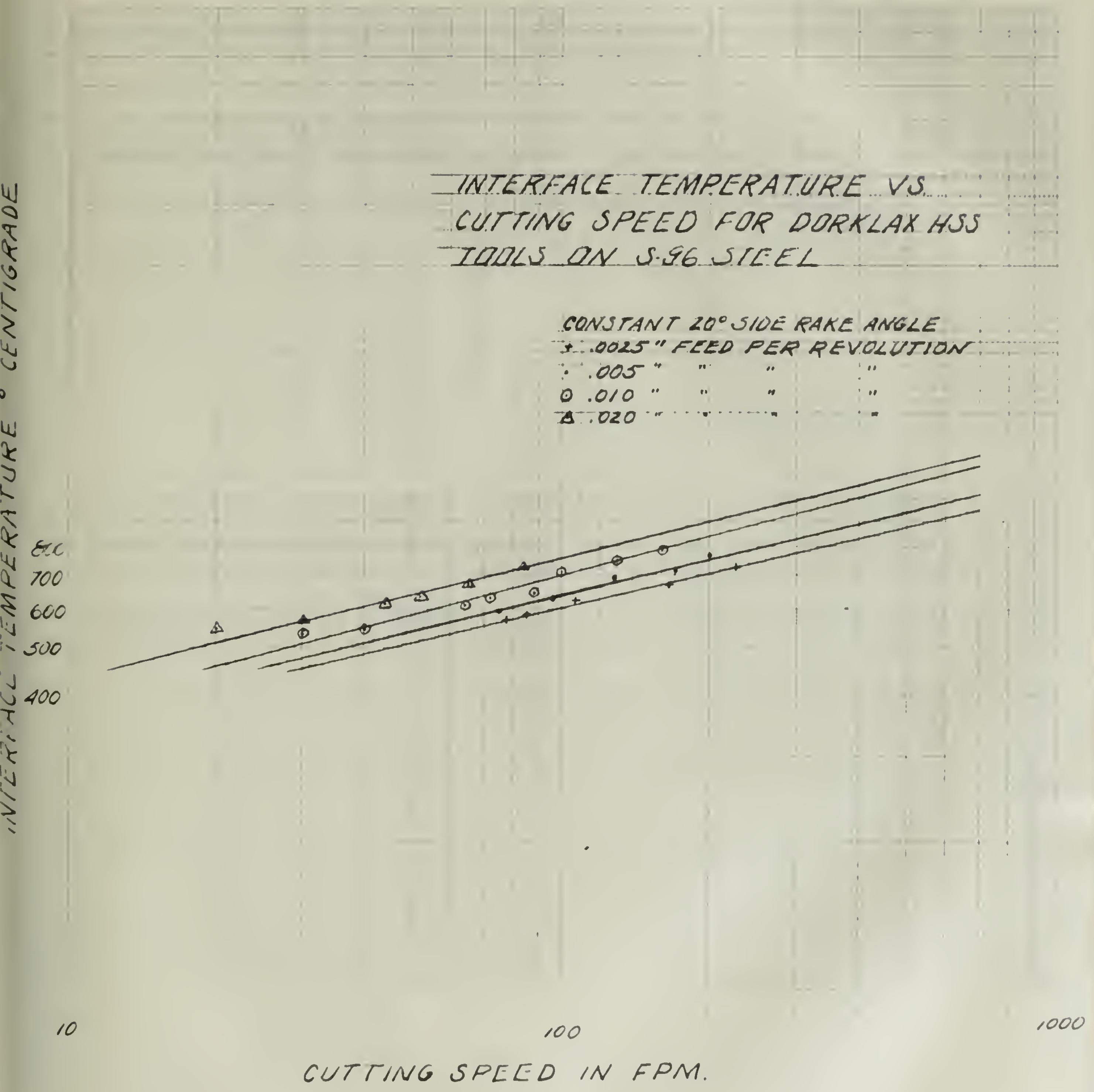

INTERFACE TEMPERATURE VS. TOOL LIFE FOR DORKLAX HSS TOOLS ON S-96 STEEL
+ .0025" FEED PER REVOLUTION
• .005" " " "
⊙ .010" " " "
△ .020" " " "
CONSTANT 20° SIDE RAKE ANGLE
INTERFACE TEMPERATURE IN ° CENTIGRADE
900
800
700
600
500
400
1
10
100
TOOL LIFE IN MINUTES

App(a) 6

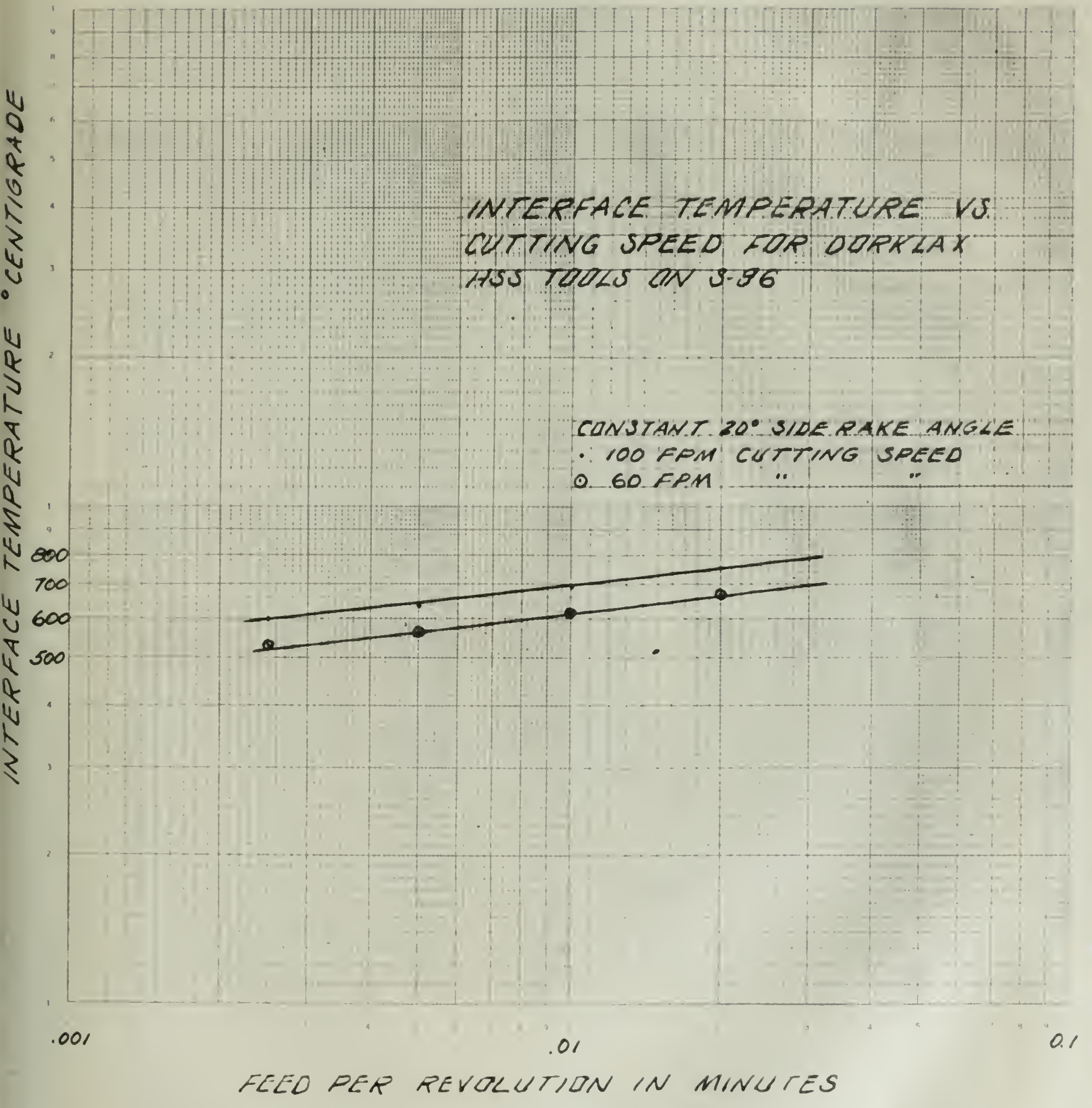


INTERFACE TEMPERATURE VS. TOOL LIFE
FOR DORKLAX HSS TOOLS ON S-96 STEEL

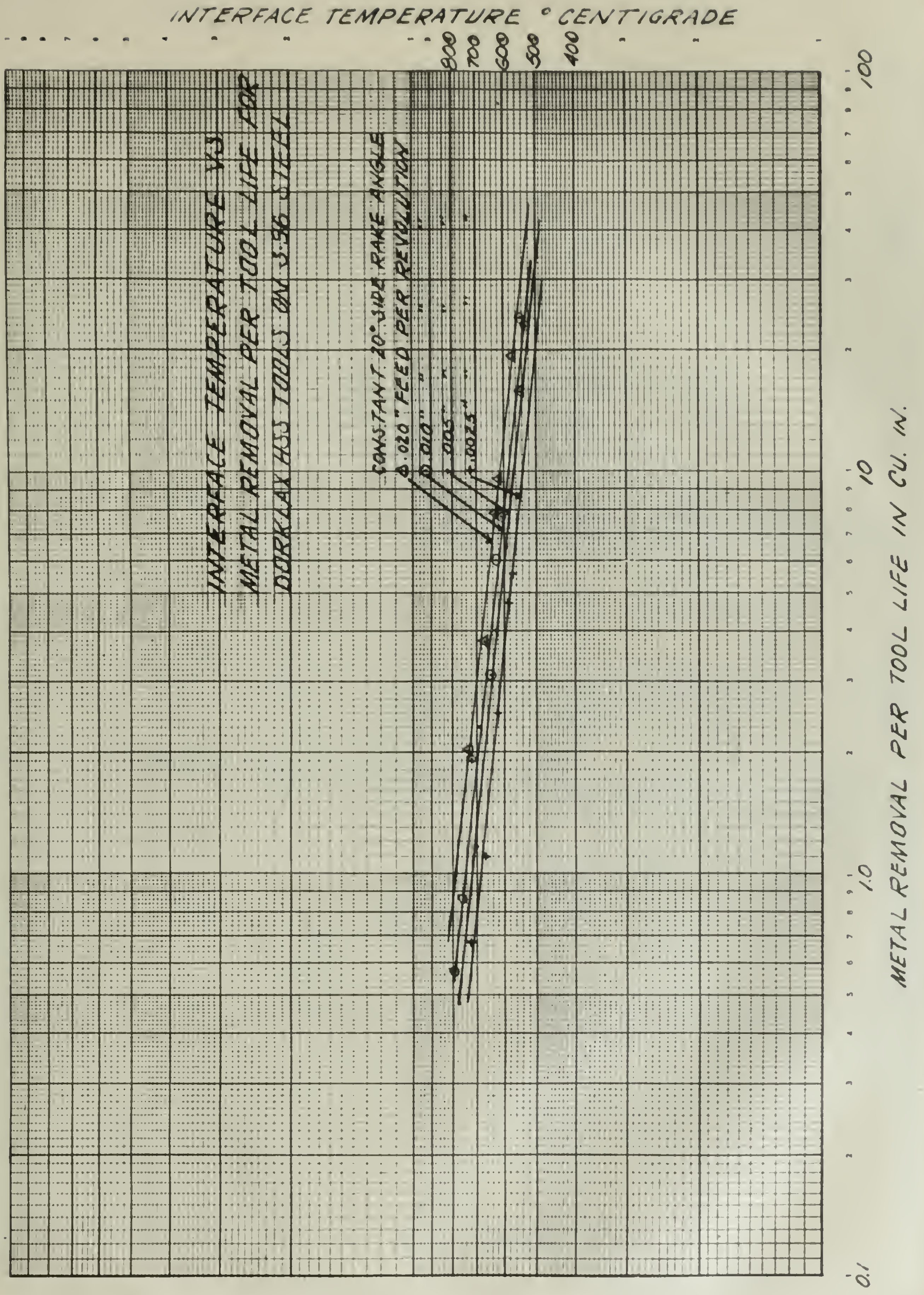
App(a) 7

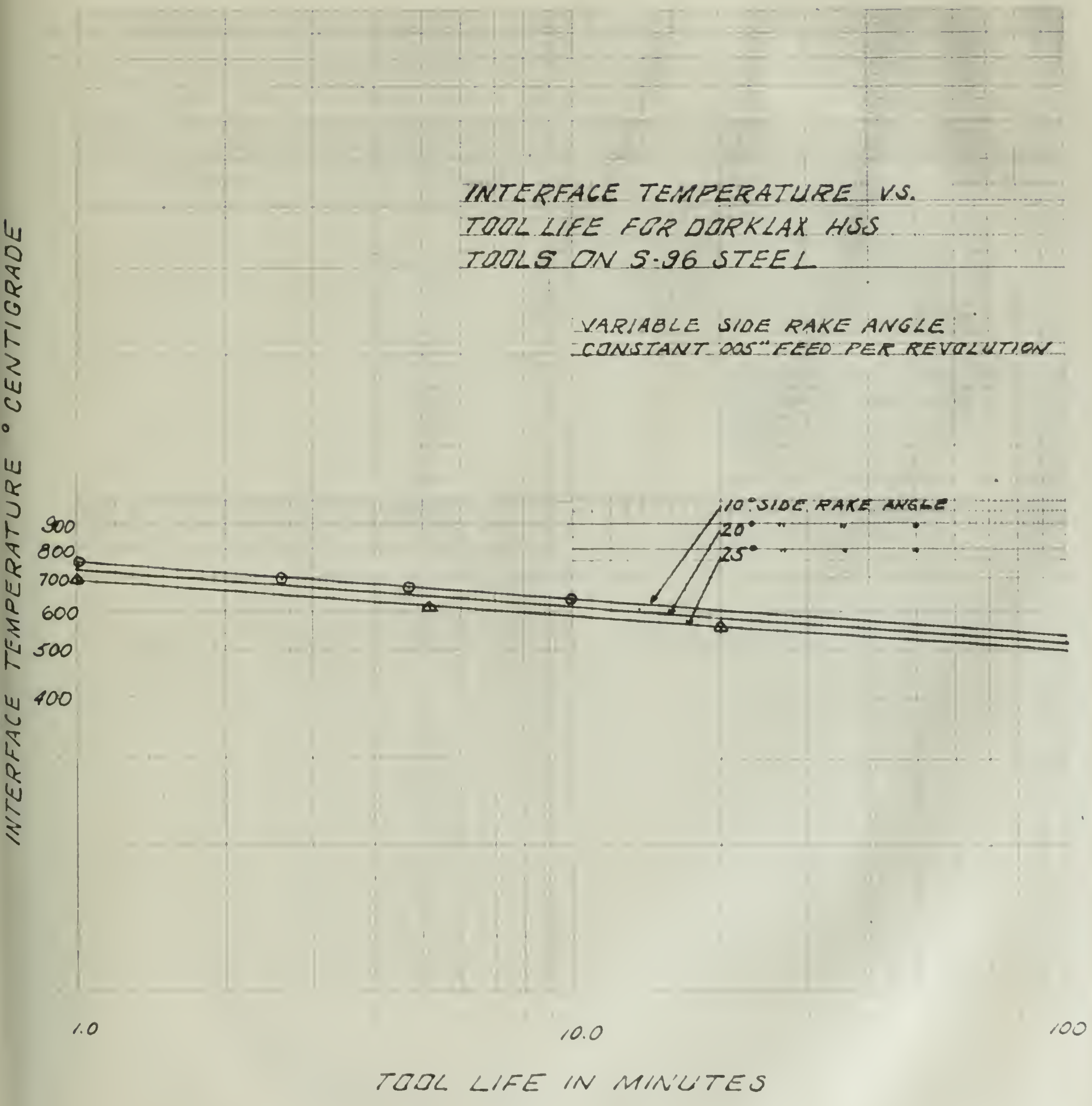

INTERFACE TEMPERATURE VS. CUTTING SPEED FOR DORKLAX HSS TOOLS ON S-96 STEEL
CONSTANT 20° SIDE RAKE ANGLE
+ .0025" FEED PER REVOLUTION
• .005 " " " "
⊙ .010 " " " "
Δ .020 " " " "
INTERFACE TEMPERATURE ° CENTIGRADE
800
700
600
500
400
10
100
1000
CUTTING SPEED IN FPM.

App'(a)


INTERFACE TEMPERATURE VS CUTTING SPEED FOR DORKIAX HSS TOOLS ON S-96
CONSTANT 20° SIDE RAKE ANGLE
· 100 FPM CUTTING SPEED
⊙ 60 FPM " "
INTERFACE TEMPERATURE °CENTIGRADE
800
700
600
500
.001
.01
0.1
FEED PER REVOLUTION IN MINUTES

App(a) 2

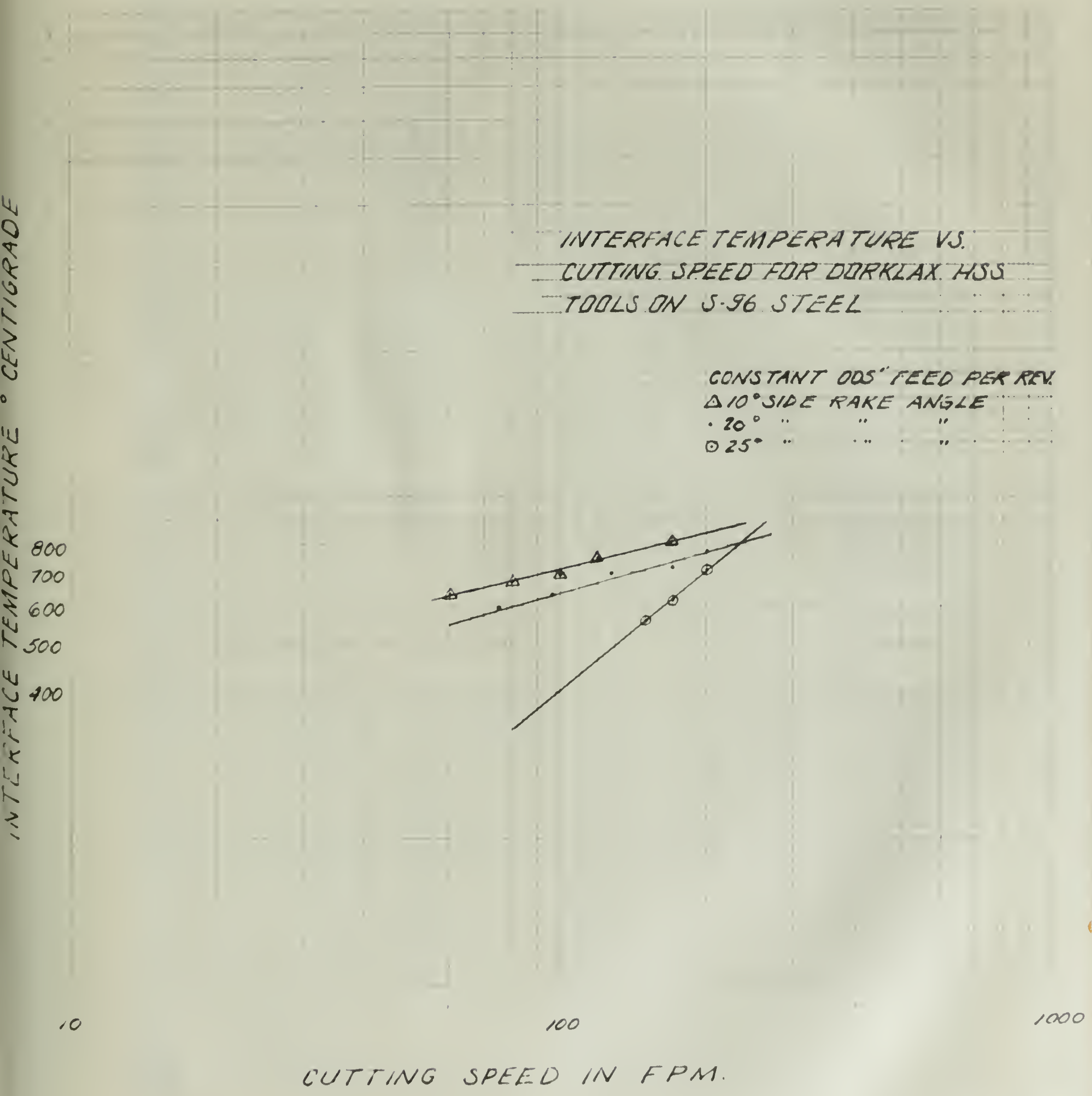

INTERFACE TEMPERATURE °CENTIGRADE
800
700
600
500
400
INTERFACE TEMPERATURE VS
METAL REMOVAL PER TOOL LIFE FOR
DORKLAX HSS TOOLS ON 3.96 STEEL
CONSTANT 20° SIDE RAKE ANGLE
Δ .020" FEED PER REVOLUTION
⊙ .010" " " "
× .005" " " "
+ .0025" " " "
0.1
1.0
10
100
METAL REMOVAL PER TOOL LIFE IN CU. IN.

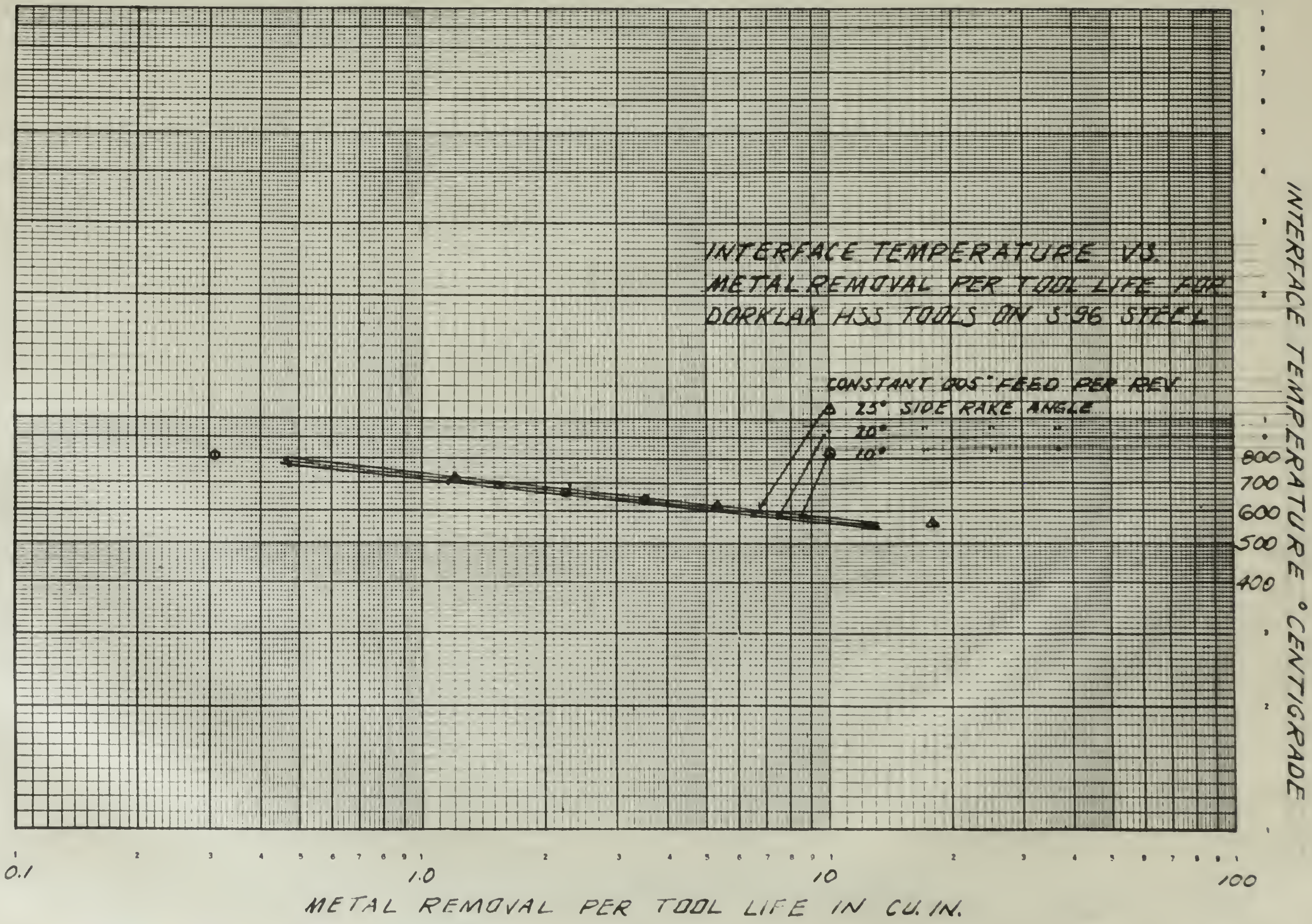


INTERFACE TEMPERATURE VS. TOOL LIFE FOR DORKLAX HSS TOOLS ON S-96 STEEL

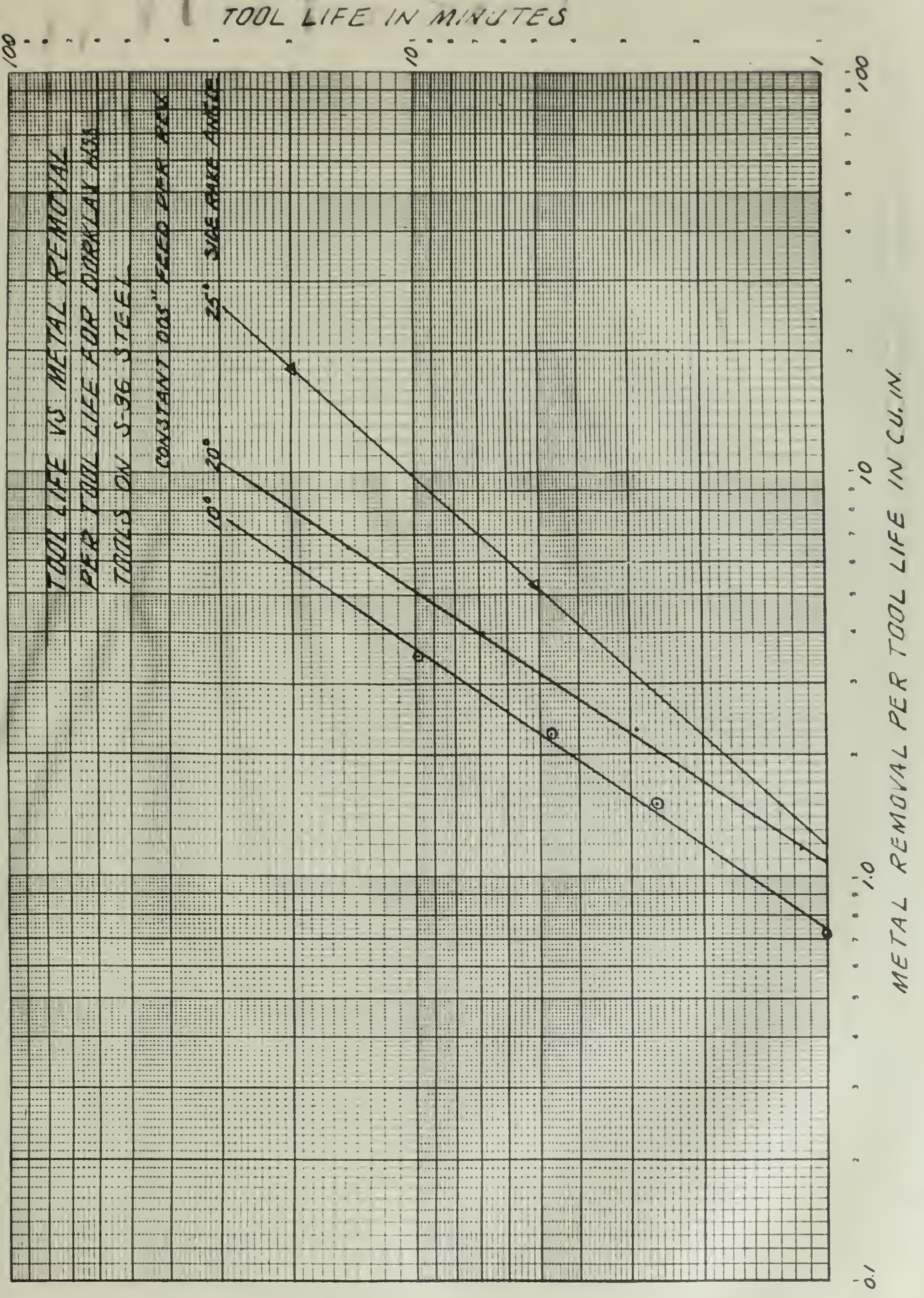
VARIABLE SIDE RAKE ANGLE
CONSTANT .005" FEED PER REVOLUTION

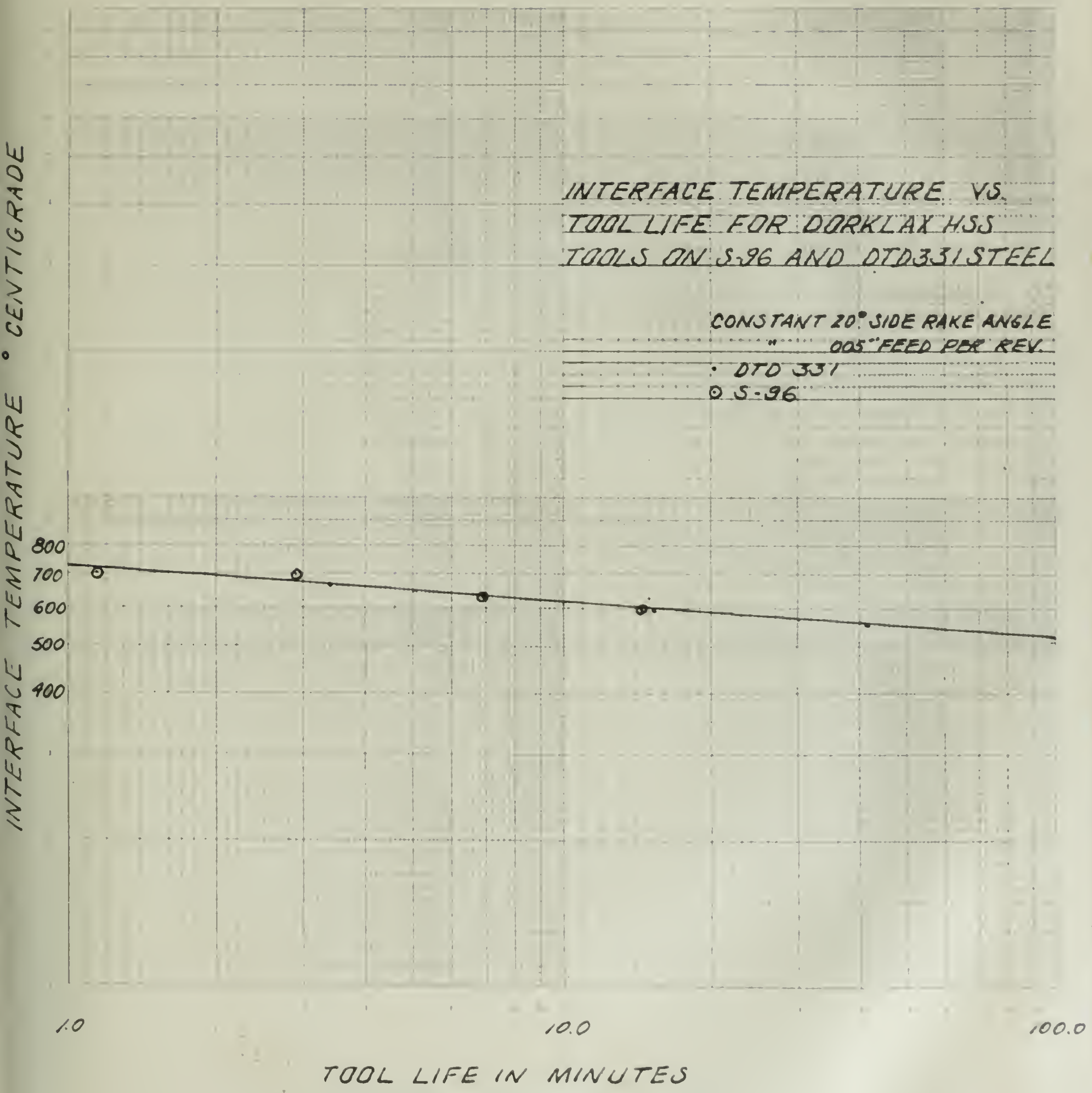
[illegible] 11

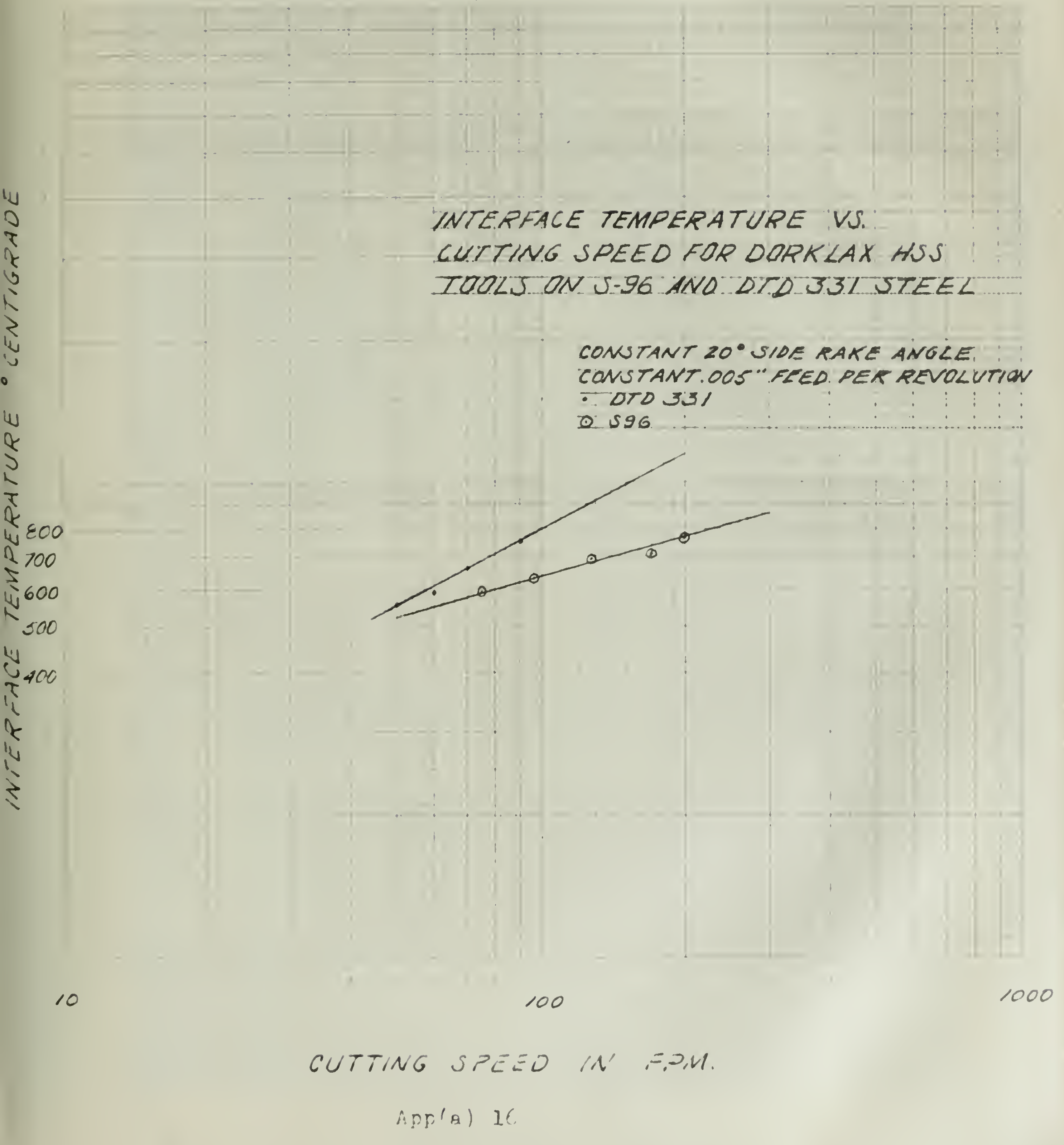

INTERFACE TEMPERATURE VS. CUTTING SPEED FOR DORKLAX HSS TOOLS ON S-96 STEEL
CONSTANT .005" FEED PER REV.
△ 10° SIDE RAKE ANGLE
• 20° " " "
⊙ 25° " " "
INTERFACE TEMPERATURE ° CENTIGRADE
800
700
600
500
400
10
100
1000
CUTTING SPEED IN FPM.

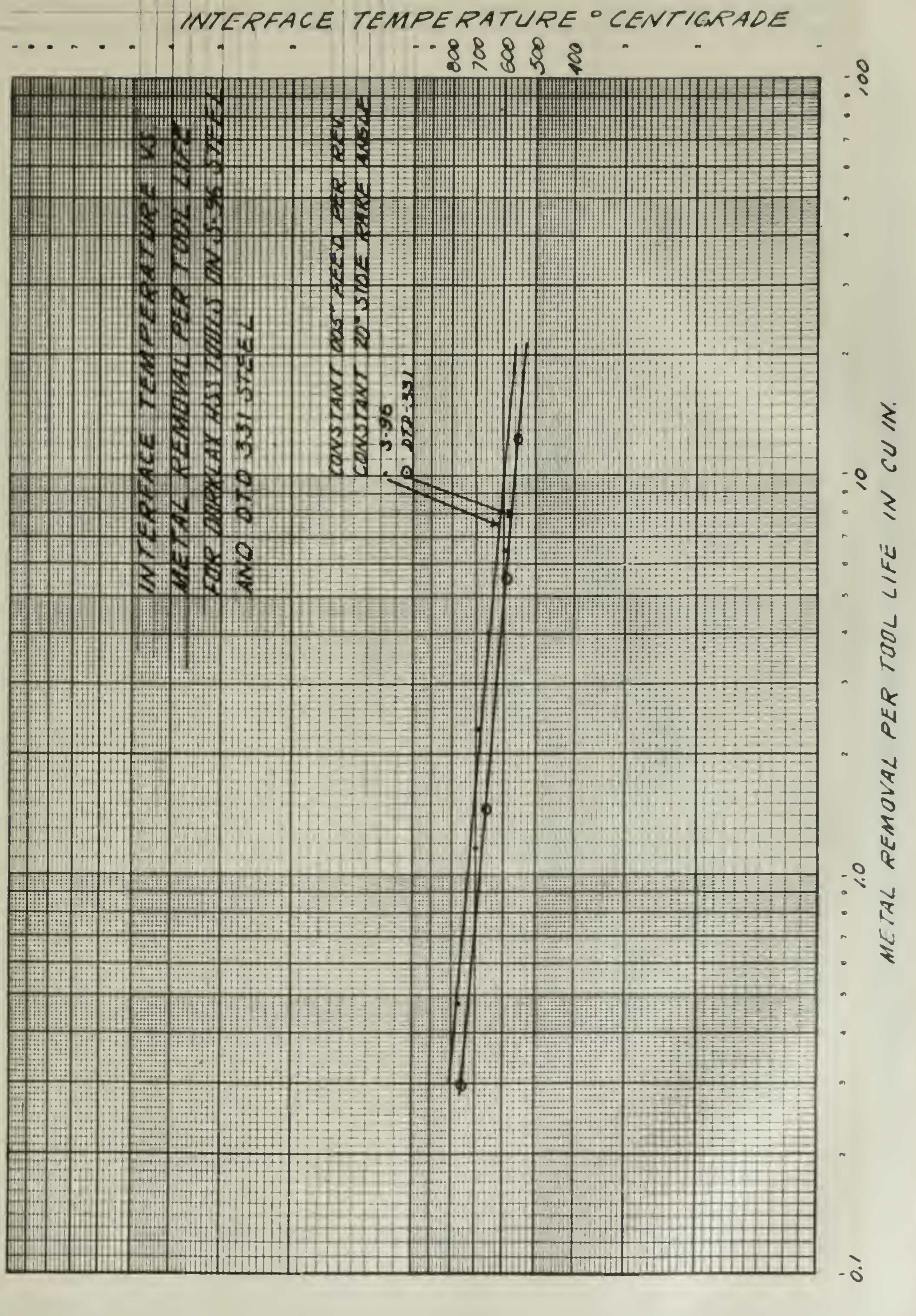


INTERFACE TEMPERATURE VS.
METAL REMOVAL PER TOOL LIFE FOR
DORKLAX HSS TOOLS ON S-96 STEEL

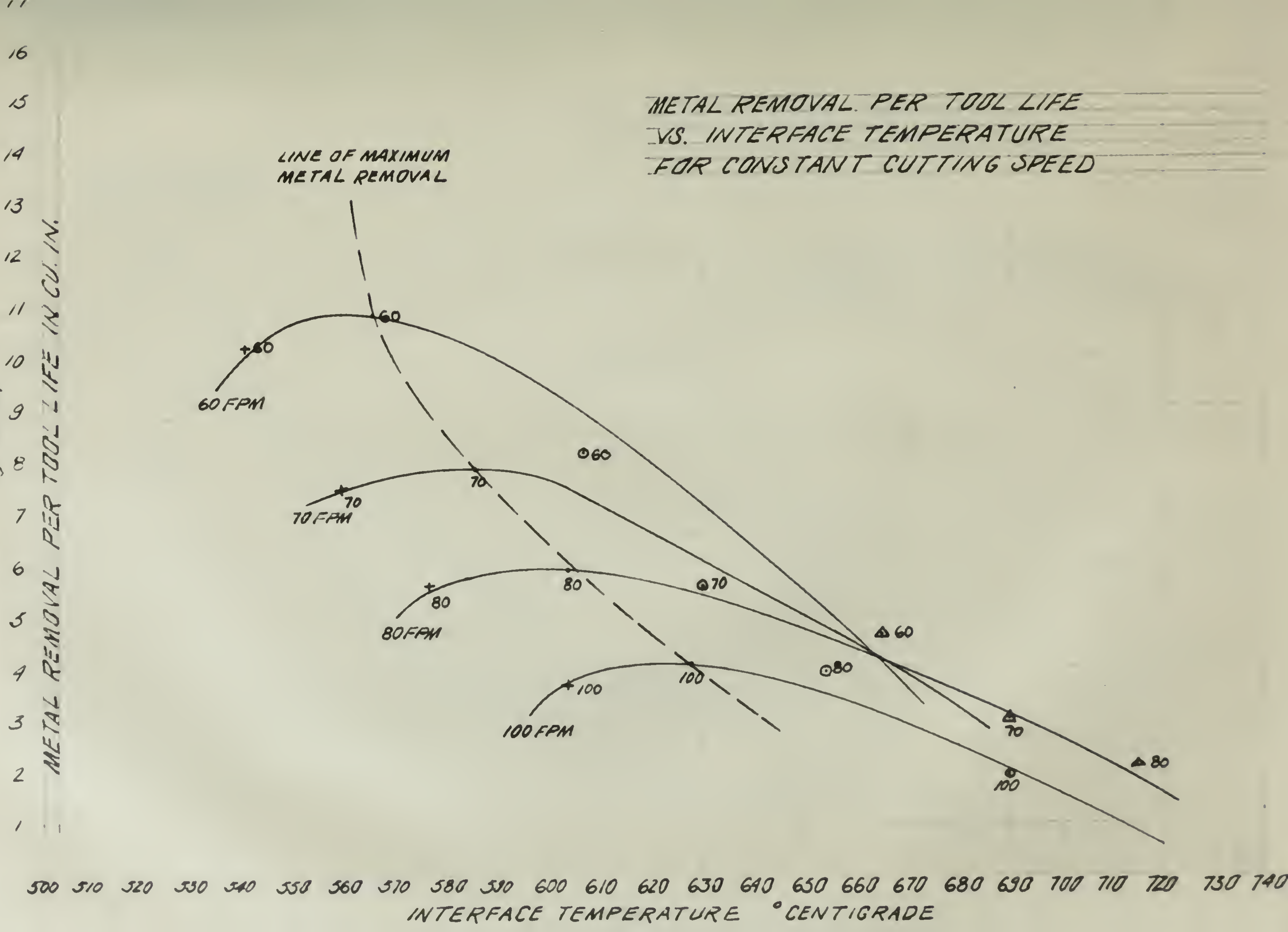
TOOL LIFE IN MINUTES
100
10
1
TOOL LIFE VS METAL REMOVAL
PER TOOL LIFE FOR DURALAY ABA
TOOLS ON S-96 STEEL
CONSTANT .005" FEED PER REV.
25° SIDE RAKE ANGLE
20°
10°
METAL REMOVAL PER TOOL LIFE IN CU. IN.
100
10
1.0
0.1

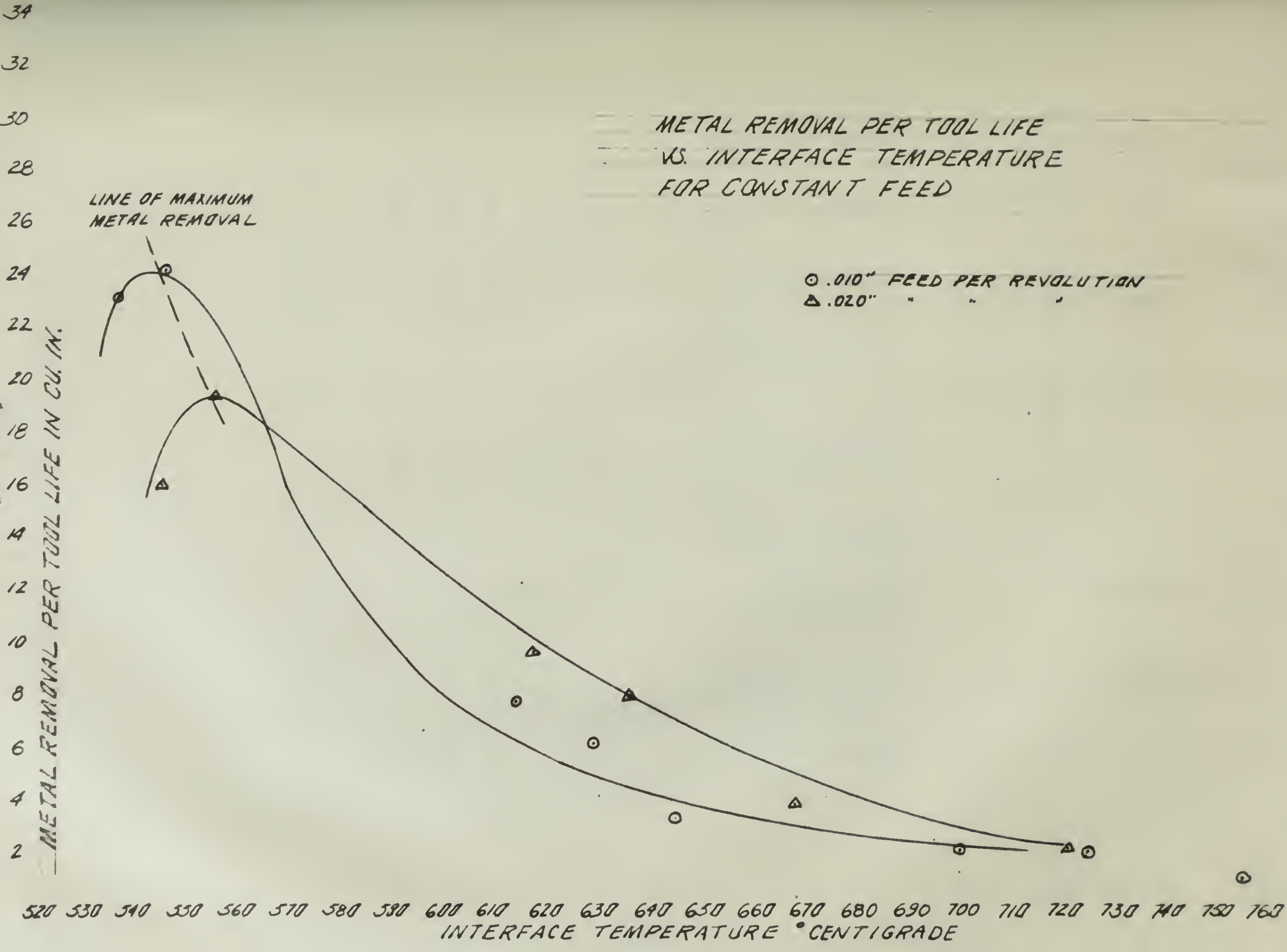

INTERFACE TEMPERATURE VS. TOOL LIFE FOR DORKLAX HSS TOOLS ON S-96 AND DTD331 STEEL
CONSTANT 20° SIDE RAKE ANGLE
" .005" FEED PER REV.
· DTD 331
⊙ S-96
INTERFACE TEMPERATURE ° CENTIGRADE
800
700
600
500
400
1.0
10.0
100.0
TOOL LIFE IN MINUTES


INTERFACE TEMPERATURE VS.
CUTTING SPEED FOR DORKLAX HSS
TOOLS ON S-96 AND DTD 331 STEEL
CONSTANT 20° SIDE RAKE ANGLE
CONSTANT .005" FEED PER REVOLUTION
• DTD 331
⊙ S96
INTERFACE TEMPERATURE °CENTIGRADE
800
700
600
500
400
10
100
1000
CUTTING SPEED IN F.P.M.


INTERFACE TEMPERATURE VS
METAL REMOVAL PER TOOL LIFE
FOR ORMKLAY HSS TOOLS ON S-96 STEEL
AND DTD 331 STEEL
CONSTANT .005" FEED PER REV.
CONSTANT 20° SIDE RAKE ANGLE
S-96
DTD-331
INTERFACE TEMPERATURE ° CENTIGRADE
800
700
600
500
400
METAL REMOVAL PER TOOL LIFE IN CU IN.
0.1
1.0
10
100

METAL REMOVAL PER TOOL LIFE
VS. INTERFACE TEMPERATURE
FOR CONSTANT CUTTING SPEED





METAL REMOVAL PER TOOL LIFE
VS. INTERFACE TEMPERATURE
FOR CONSTANT FEED

PREDICTION OF TOOL LIFE

DATA:

(For DORKLAX tools cutting S-96).

| SIDE RAKE ANGLE | FEED IN./REV. | CUTTING SPEED FPM | TOOL LIFE (MIN.) | METAL REMOVED CU.IN. | TEMPERATURE SCALE RDG. (MV.) | TEMPERATURE °C | CUTTING FORCE SCALE RDG. | CUTTING FORCE LBS. |
|---|---|---|---|---|---|---|---|---|
| 20° | .0025 | 85 | 18.40 | 4.70 | 11.0 | 584 | 13.7 | 114 |
| | .0025 | 226 | .98 | .67 | 14.3 | 722 | 13.5 | 112 |
| | .0025 | 167 | 2.21 | 1.10 | 12.9 | 668 | 13.8 | 115 |
| | .0025 | 108 | 7.70 | 2.50 | 11.9 | 625 | 14.2 | 118 |
| | .0025 | 77 | 24.12 | 5.56 | 10.7 | 575 | 14.9 | 124 |
| | .005 | 170 | 1.15 | 1.17 | 13.7 | 700 | 20.2 | 168 |
| | .005 | 128 | 2.90 | 2.30 | 13.6 | 695 | 20.8 | 173 |
| | .005 | 96 | 6.93 | 4.00 | 12.0 | 630 | 20.7 | 172 |
| | .005 | 75 | 14.52 | 6.51 | 11.2 | 596 | 21.0 | 175 |
| | .005 | 200 | .40 | .48 | 15.5 | 771 | 19.8 | 165 |

PREDICTION OF TOOL LIFE

DATA:

(For DORKLAX tools cutting S-96).

| SIDE RAKE ANGLE | FEED IN./REV. | CUTTING SPEED FPM. | TOOL LIFE (MIN.) | METAL REMOVED CU.IN. | TEMPERATURE SCALE RDG. MV. | TEMPERATURE °C | CUTTING FORCE SCALE RDG. | CUTTING FORCE LBS. |
|---|---|---|---|---|---|---|---|---|
| 20° | .010 | 40 | 50.11 | 24.02 | 10.0 | 545 | 41.2 | 344 |
| | .010 | 30 | 64.10 | 23.06 | 9.8 | 536 | 42.4 | 353 |
| | .010 | 88 | 3.13 | 3.16 | 12.4 | 645 | 39.5 | 329 |
| | .010 | 72 | 7.00 | 6.00 | 12.0 | 629 | 42.0 | 350 |
| | .010 | 64 | 10.11 | 7.66 | 11.6 | 614 | 42.5 | 354 |
| | .010 | 130 | .55 | .86 | 15.1 | 755 | 37.0 | 308 |
| | .010 | 100 | 1.62 | 1.92 | 14.4 | 725 | 41.5 | 346 |
| | .010 | 160 | .33 | .57 | 15.9 | 787 | 40.1 | 334 |
| | .020 | 84 | 1.00 | 2.01 | 14.3 | 722 | 72.6 | 605 |
| | .020 | 65 | 2.39 | 3.74 | 13.0 | 668 | 73.1 | 610 |
| | .020 | 54 | 5.92 | 7.80 | 12.2 | 636 | 73.3 | 611 |
| | .020 | 44 | 9.03 | 9.50 | 11.7 | 617 | 72.8 | 606 |
| | .020 | 30 | 39.60 | 19.30 | 10.2 | 555 | 74.5 | 621 |
| | .020 | 20 | 53.00 | 15.90 | 10.0 | 545 | 77.6 | 646 |

PREDICTION OF TOOL LIFE

DATA:

(For PORFLAX tools cutting B-96)

| SIDE RAKE ANGLE | FEED IN./REV. | CUTTING SPEED FPM | TOOL LIFE (MIN.) | METAL REMOVED (CU. IN.) | TEMPERATURE SCALE RDG MV. | TEMPERATURE °C | CUTTING FORCE SCALE RDG | CUTTING FORCE LBS. |
|---|---|---|---|---|---|---|---|---|
| 10° | .005 | 60 | 9.90 | 3.50 | 12.1 | 634 | 33.5 | 279 |
| 10° | | 100 | 2.61 | 1.56 | 13.7 | 696 | 36.0 | 300 |
| 10° | | 120 | 1.01 | .72 | 14.8 | 743 | 31.4 | 262 |
| 10° | | 170 | .32 | .31 | 16.4 | 807 | 31.7 | 264 |
| 10° | | 80 | 4.72 | 2.25 | 12.9 | 666 | 32.9 | 274 |
| 25° | | 200 | .85 | 1.02 | 13.9 | 706 | 18.3 | 152 |
| 25° | | 170 | 5.17 | 5.26 | 11.7 | 616 | 18.5 | 154 |
| 25° | | 150 | 20.00 | 18.00 | 10.3 | 558 | 19.0 | 158 |
| 15° | | 80 | 11.05 | 5.28 | | | 23.5 | 196 |
| 15° | | 200 | .30 | .30 | | | 22.7 | 189 |
| 15° | | 100 | 5.00 | 3.00 | | | 22.6 | 188 |
| 15° | | 130 | 1.50 | 1.17 | | | 21.7 | 181 |
| 30° | | 100 | 10.04 | 6.02 | | | 18.9 | 158 |
| 30° | | 130 | 2.63 | 2.03 | | | 18.9 | 158 |
| 30° | | 160 | 1.10 | 1.05 | | | 19.3 | 161 |
| 30° | | 200 | .35 | .42 | | | 19.5 | 162 |

(EQUIVALENT CONTINUOUS CUTTING TIME PER TOOTH) IN MIN.

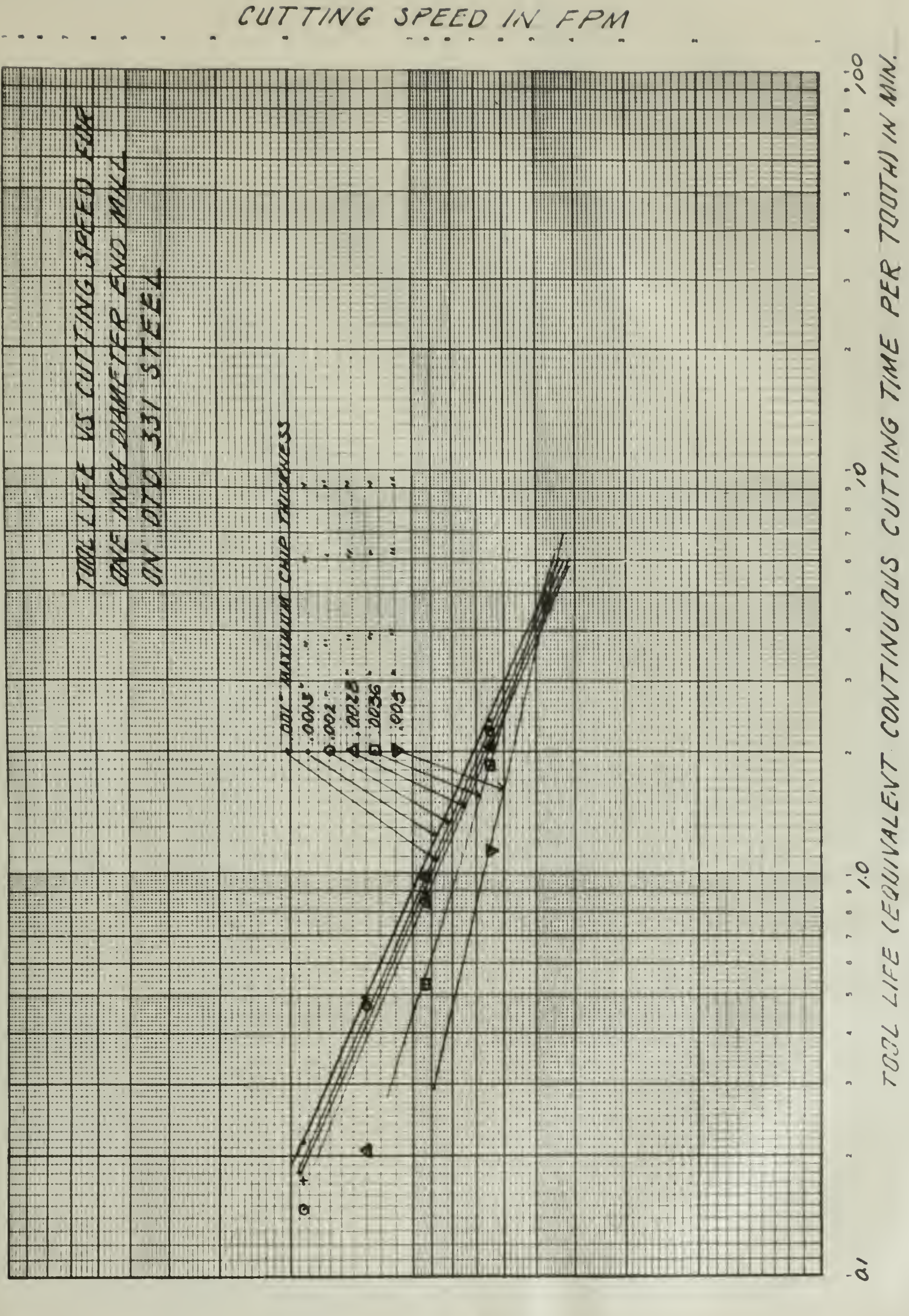
PREDICTION OF TOOL LIFE

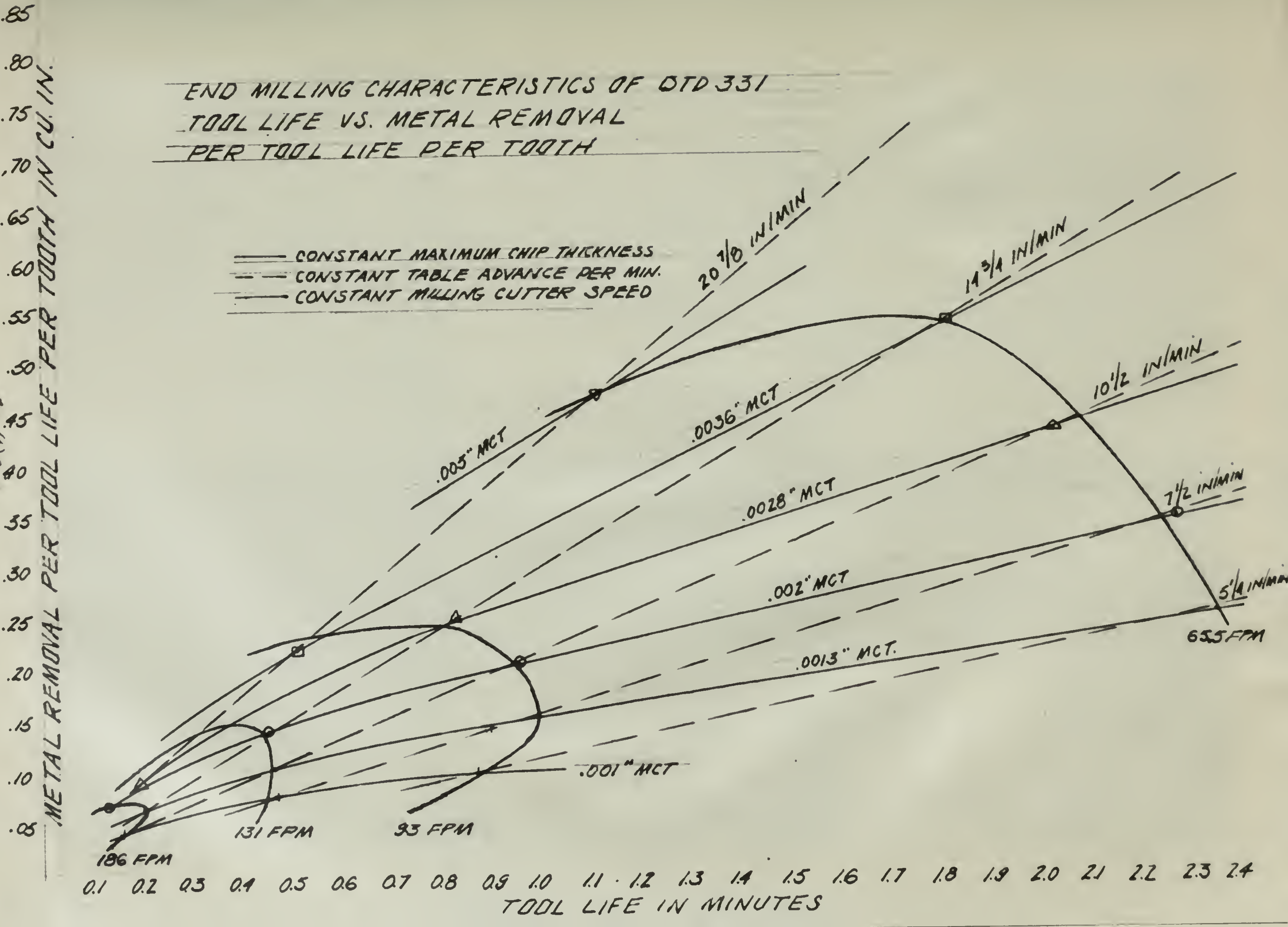
DATA:

(For DORMILAX tools cutting DTD 331).

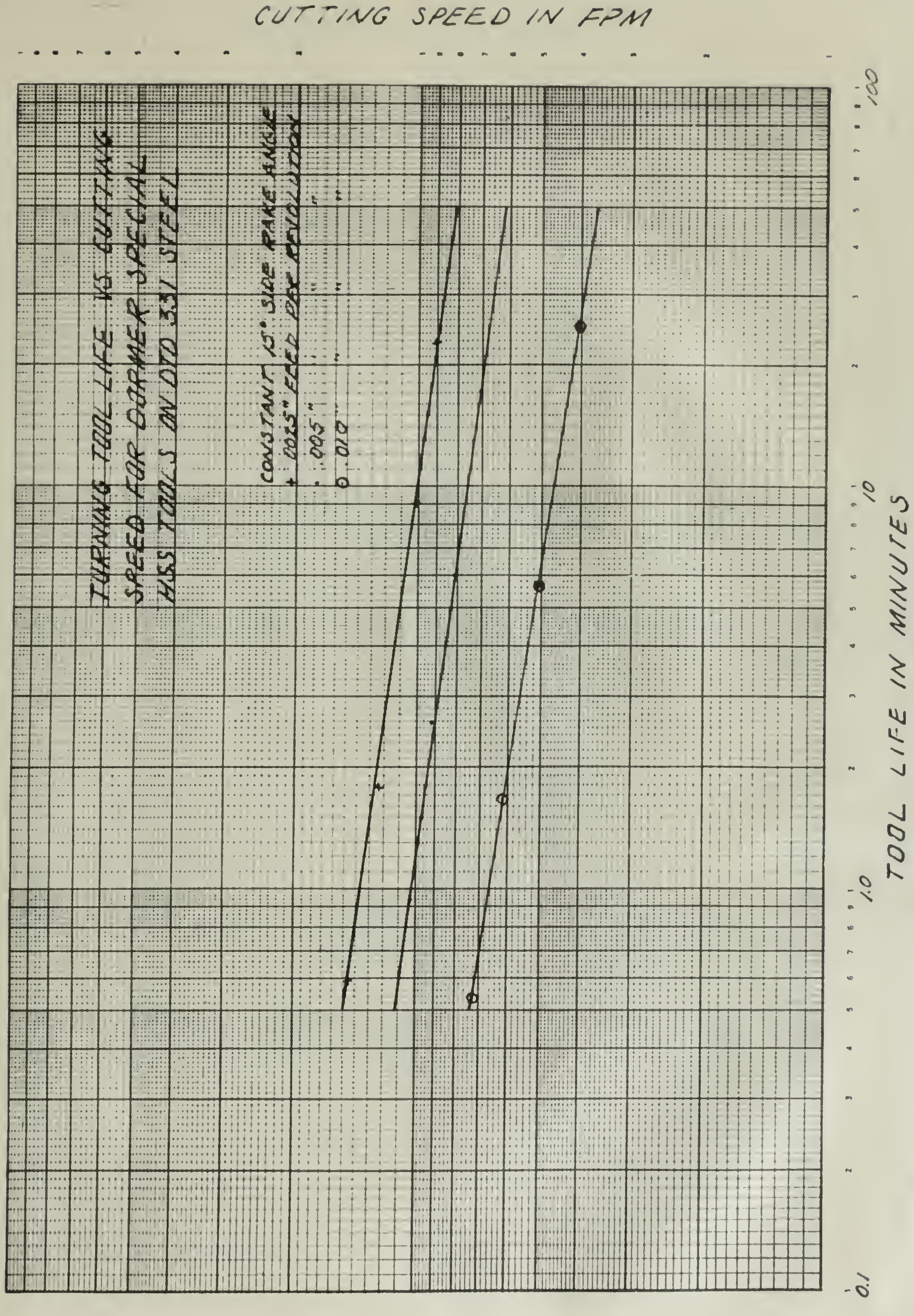
| SIDE RAKE ANGLE | FEED IN/REV | CUTTING SPEED FPM | TOOL LIFE (MIN.) | METAL REMOVED (CU.IN.) | TEMPERATURE SCALE RDGS. MV | TEMPERATURE °C | CUTTING FORCE SCALE RDGS. | CUTTING FORCE LBS. |
|---|---|---|---|---|---|---|---|---|
| 20° | .005 | 90 | .55 | .30 | 20.4 | 754 | 31.4 | 262 |
| | .005 | 70 | 3.40 | 1.43 | 16.7 | 650 | 32.0 | 267 |
| | .005 | 60 | 15.31 | 5.51 | 15.1 | 605 | 33.1 | 276 |
| | .005 | 50 | 41.20 | 1.24 | 13.0 | 544 | 35.6 | 296 |
| | .010 | 60 | .40 | .29 | 21.0 | 771 | 51.5 | 429 |
| | .010 | 50 | 1.55 | .90 | 19.0 | 714 | 52.2 | 435 |
| | .010 | 40 | 7.77 | 3.74 | 15.6 | 620 | 53.0 | 441 |
| | .010 | 35 | 18.10 | 7.61 | 14.5 | 588 | 56.7 | 473 |

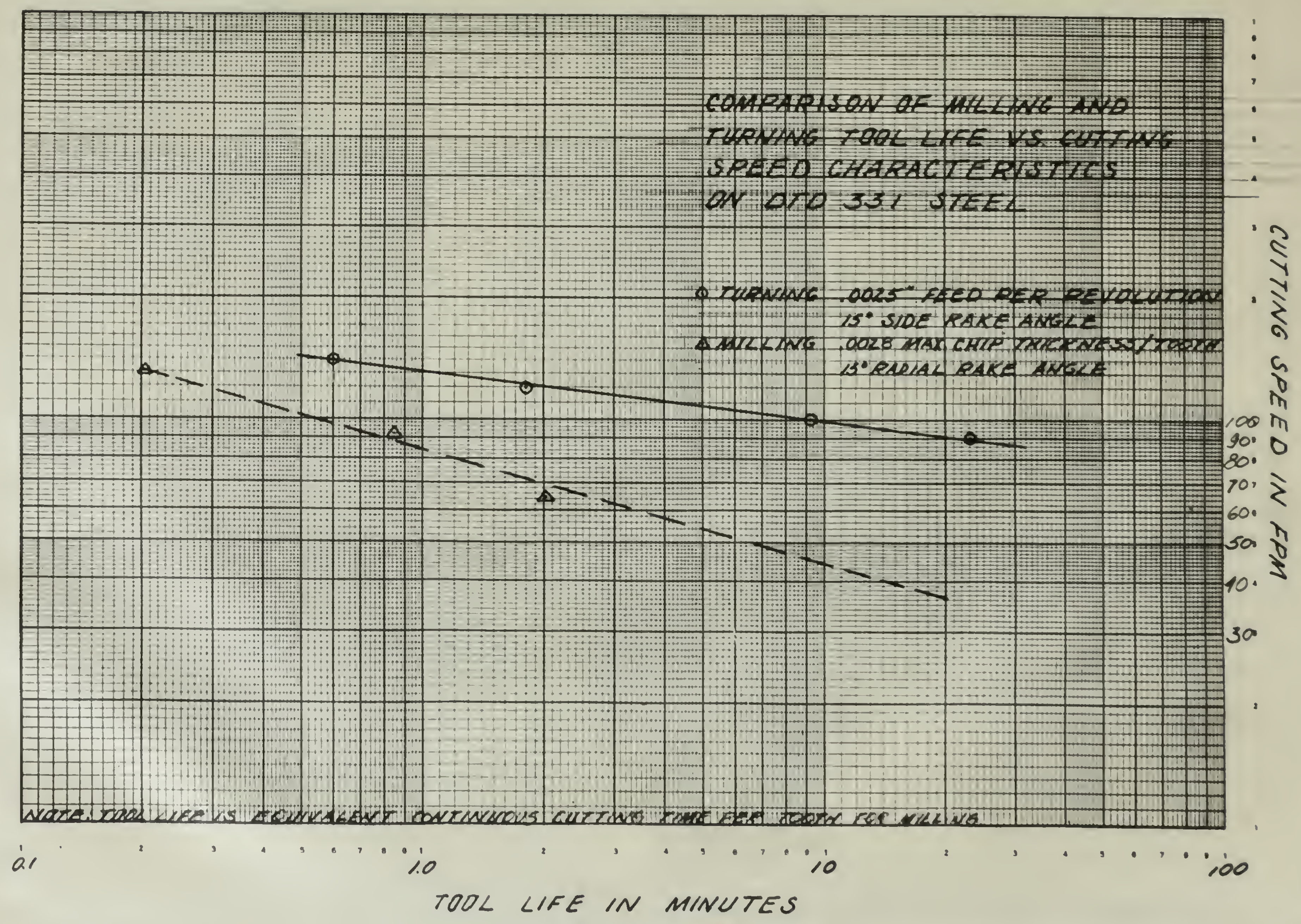
TOOL LIFE (EQUIVALENT CONTINUOUS CUTTING TIME PER TOOTH) IN MIN.

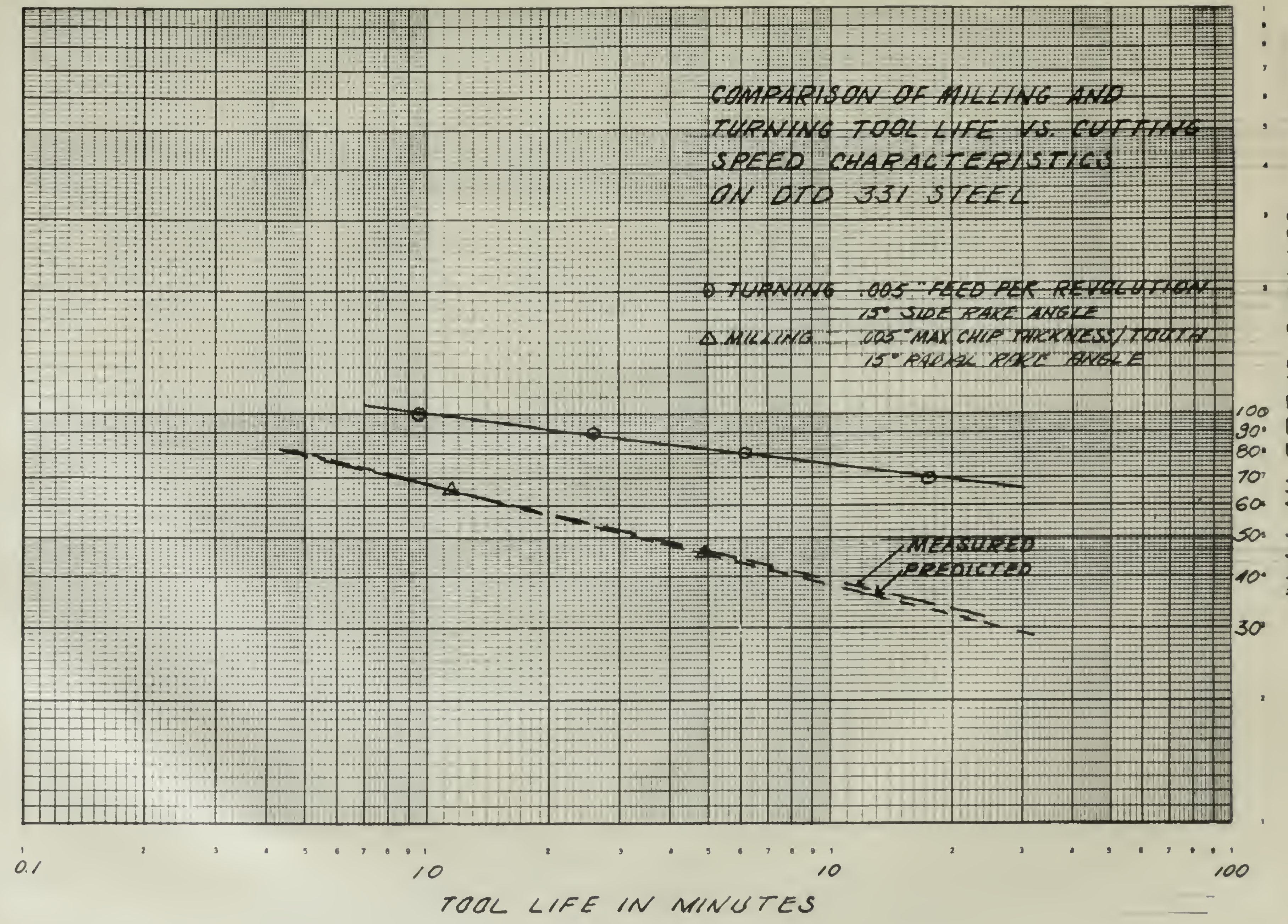

CUTTING SPEED IN FPM
TOOL LIFE VS CUTTING SPEED FOR
ONE INCH DIAMETER END MILL
ON OTD 331 STEEL
● .001 - MAXIMUM CHIP THICKNESS
+ .0015 "
○ .002 "
△ .0028 "
□ .0036 "
▼ .005 "
.01
1.0
10
100
TOOL LIFE (EQUIVALENT CONTINUOUS CUTTING TIME PER TOOTH) IN MIN.

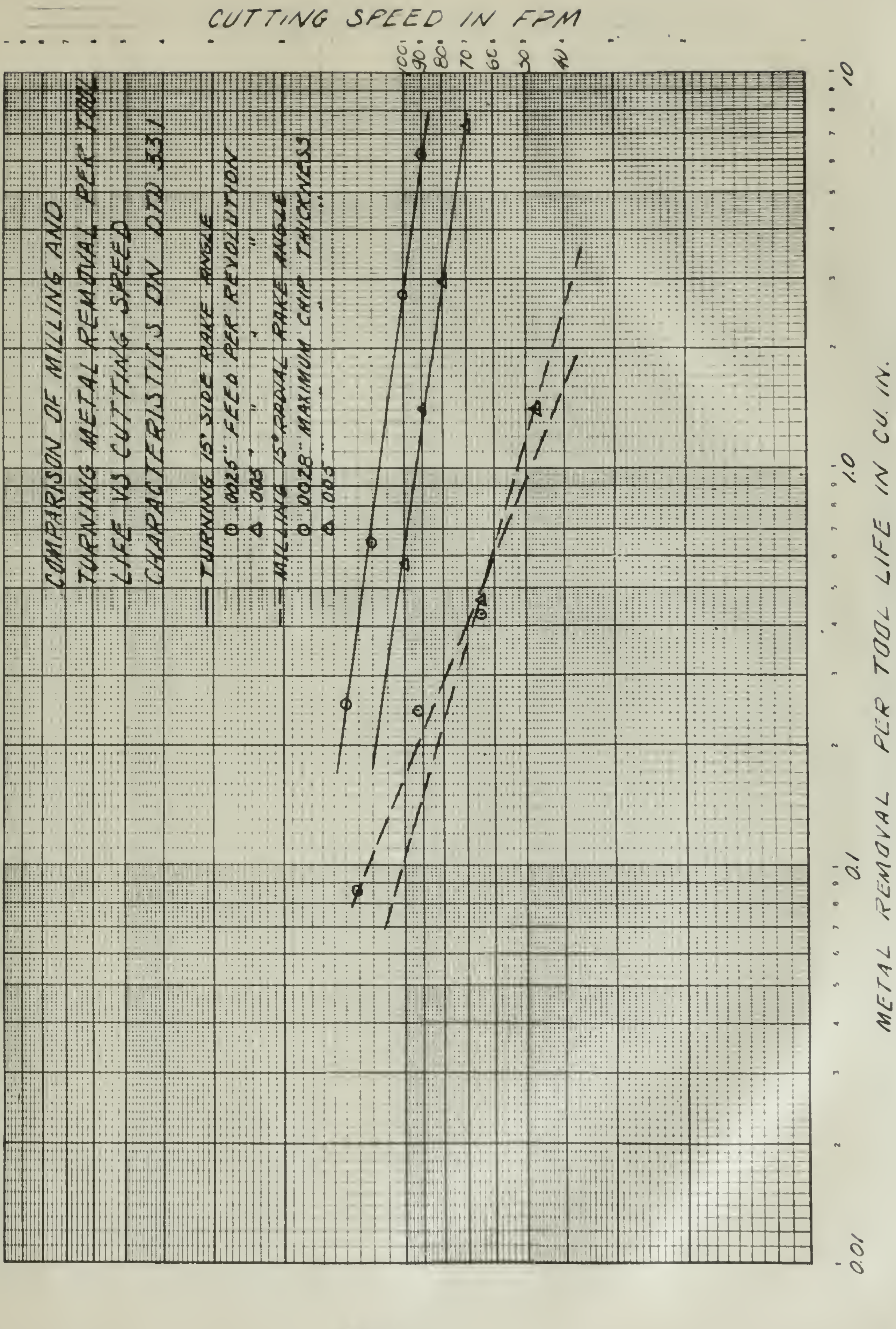


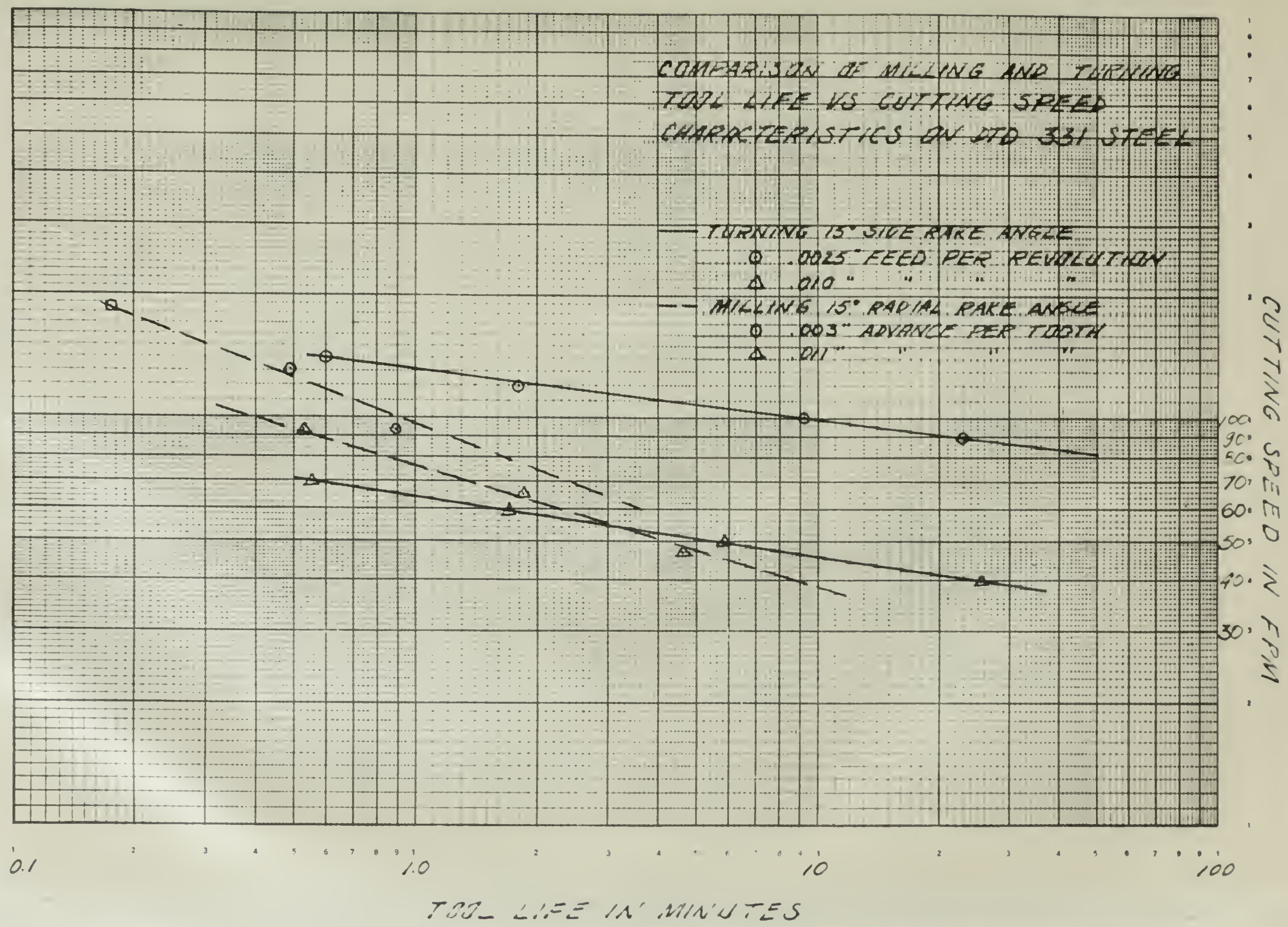
END MILLING CHARACTERISTICS OF OTD 331
TOOL LIFE VS. METAL REMOVAL
PER TOOL LIFE PER TOOTH


CUTTING SPEED IN FPM
TURNING TOOL LIFE VS. CUTTING SPEED FOR DORMER SPECIAL HSS TOOLS ON DTD 331 STEEL
CONSTANT 15° SIDE RAKE ANGLE
+ .0025" FEED PER REVOLUTION
• .005" " "
o .010 " "
0.1
1.0
10
100
TOOL LIFE IN MINUTES


COMPARISON OF MILLING AND
TURNING TOOL LIFE VS CUTTING
SPEED CHARACTERISTICS
ON DTD 331 STEEL
○ TURNING .0025" FEED PER REVOLUTION
15° SIDE RAKE ANGLE
△ MILLING .0028 MAX CHIP THICKNESS/TOOTH
13° RADIAL RAKE ANGLE
NOTE: TOOL LIFE VS EQUIVALENT CONTINUOUS CUTTING TIME PER TOOTH FOR MILLING
CUTTING SPEED IN FPM
100
90
80
70
60
50
40
30
TOOL LIFE IN MINUTES
0.1
1.0
10
100

 
COMPARISON OF MILLING AND TURNING TOOL LIFE VS. CUTTING SPEED CHARACTERISTICS ON DTD 331 STEEL


COMPARISON OF MILLING AND TURNING METAL REMOVAL PER TOOL LIFE VS CUTTING SPEED CHARACTERISTICS ON DTD 331


COMPARISON OF MILLING AND TURNING
TOOL LIFE VS CUTTING SPEED
CHARACTERISTICS ON DTD 331 STEEL
TURNING 15° SIDE RAKE ANGLE
○ .0025" FEED PER REVOLUTION
△ .010 " " " "
MILLING 15° RADIAL RAKE ANGLE
○ .003" ADVANCE PER TOOTH
△ .011" " " "
CUTTING SPEED IN FPM
100
90
80
70
60
50
40
30
TOOL LIFE IN MINUTES
0.1
1.0
10
100

Appnd 7.

MILLING CHARACTERISTICS OF DTD 331

DATA: (For DORMER 1" HELICUT end milling cutter on DTD 331.)

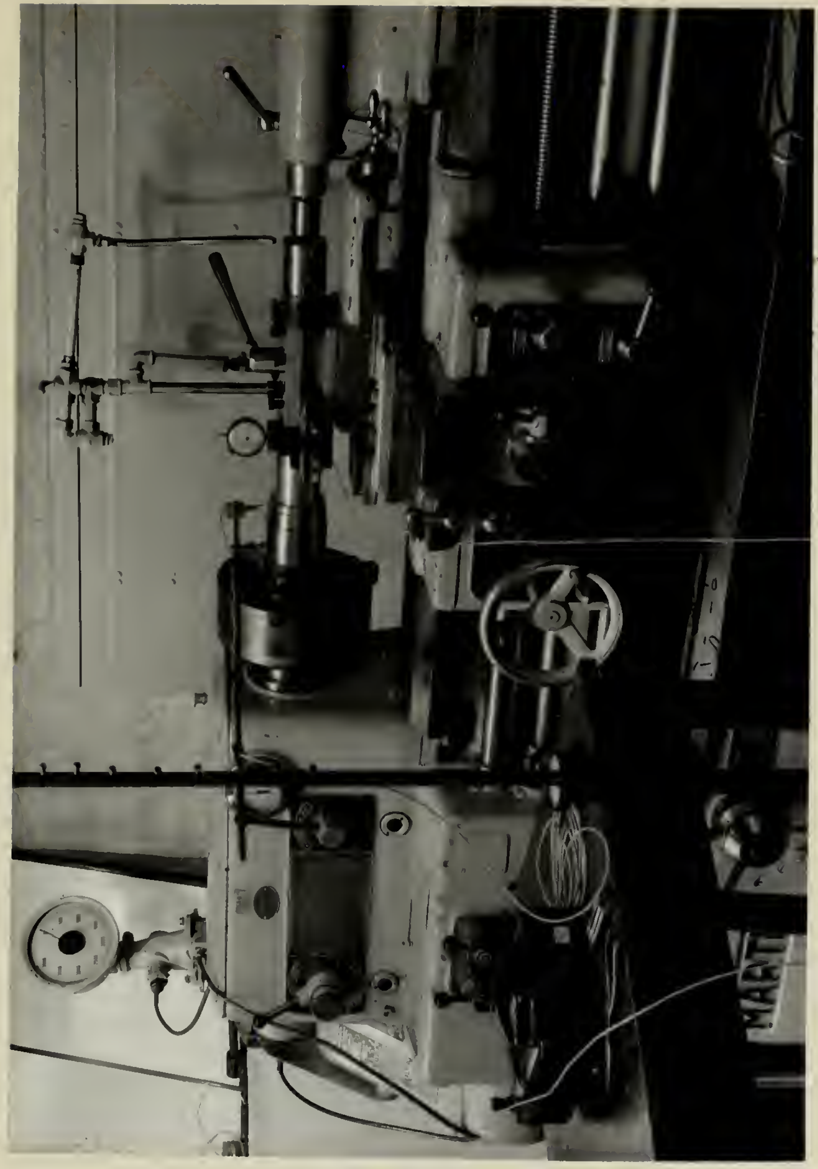
| MAXIMUM CHIP THICKNESS (IN.) | ADVANCE PER TOOTH (IN.) | CUTTER RPM | ADVANCE PER MINUTE (IN.) | CUTTING SPEED (FPM) | CUTTER LIFE (MIN.) | AVERAGE WEAR (IN.) | ADJUSTED LIFE (FOR .024" WEAR) | CONTINUOUS EQUIVALENT CUTTING TIME PER TOOTH (MIN.) | METAL REMOVAL PER TOOTH (CU.IN.) |
|---|---|---|---|---|---|---|---|---|---|
| .0010 | .0030 | 355 | 5½ | 93 | 14.0 | .0192 | 17.5 | .893 | .092 |
| .0010 | .0030 | 500 | 7½ | 131 | 5.92 | .0164 | 9.6 | .490 | .072 |
| .0010 | .0030 | 710 | 10½ | 186 | 2.33 | .0148 | 3.4 | .173 | .036 |
| .0013 | .0042 | 250 | 5½ | 65.5 | 26.60 | .0136 | 46.9 | 2.390 | .246 |
| .0013 | .0042 | 355 | 7½ | 93 | 7.50 | .0104 | 19.8 | 1.010 | .148 |
| .0013 | .0042 | 500 | 10½ | 131 | 9.42 | .0254 | 9.4 | .485 | .099 |
| .0013 | .0042 | 710 | 143/4 | 186 | 4.47 | .0256 | 4.2 | .214 | .062 |
| .0020 | .0060 | 250 | 7½ | 65.5 | 45.37 | .0240 | 45.4 | 2.260 | .340 |
| .0020 | .0060 | 355 | 10½ | 93 | 16.67 | .0212 | 19.2 | .98 | .202 |
| .0020 | .0060 | 500 | 143/4 | 131 | 13.17 | .0340 | 9.3 | .475 | .137 |
| .0020 | .0060 | 710 | 207/8 | 186 | 3.73 | .0308 | 2.9 | .148 | .061 |
| .0028 | .0084 | 250 | 10½ | 65.5 | 22.28 | .0132 | 40.5 | 2.07 | .425 |
| .0028 | .0084 | 355 | 143/4 | 93 | 13.95 | .0196 | 16.7 | .852 | .246 |
| .0028 | .0084 | 500 | 207/8 | 131 | 3.77 | .0220 | 4.1 | .209 | .086 |
| .0036 | .0118 | 180 | 10½ | 47 | 25.80 | .0068 | 90.1 | 4.600 | .944 |
| .0036 | .0118 | 250 | 143/4 | 65.5 | 33.80 | .0224 | 36.2 | 1.850 | .534 |
| .0036 | .0118 | 355 | 207/8 | 93 | 10.60 | .0254 | 10.4 | .53 | .217 |
| .0050 | .0164 | 180 | 143/4 | 47 | 15.53 | .0040 | 95.0 | 4.85 | 1.400 |
| .0050 | .0164 | 250 | 207/8 | 65.5 | 15.12 | .0164 | 22.2 | 1.13 | .464 |

## MILLING CHARACTERISTICS OF DTD 331

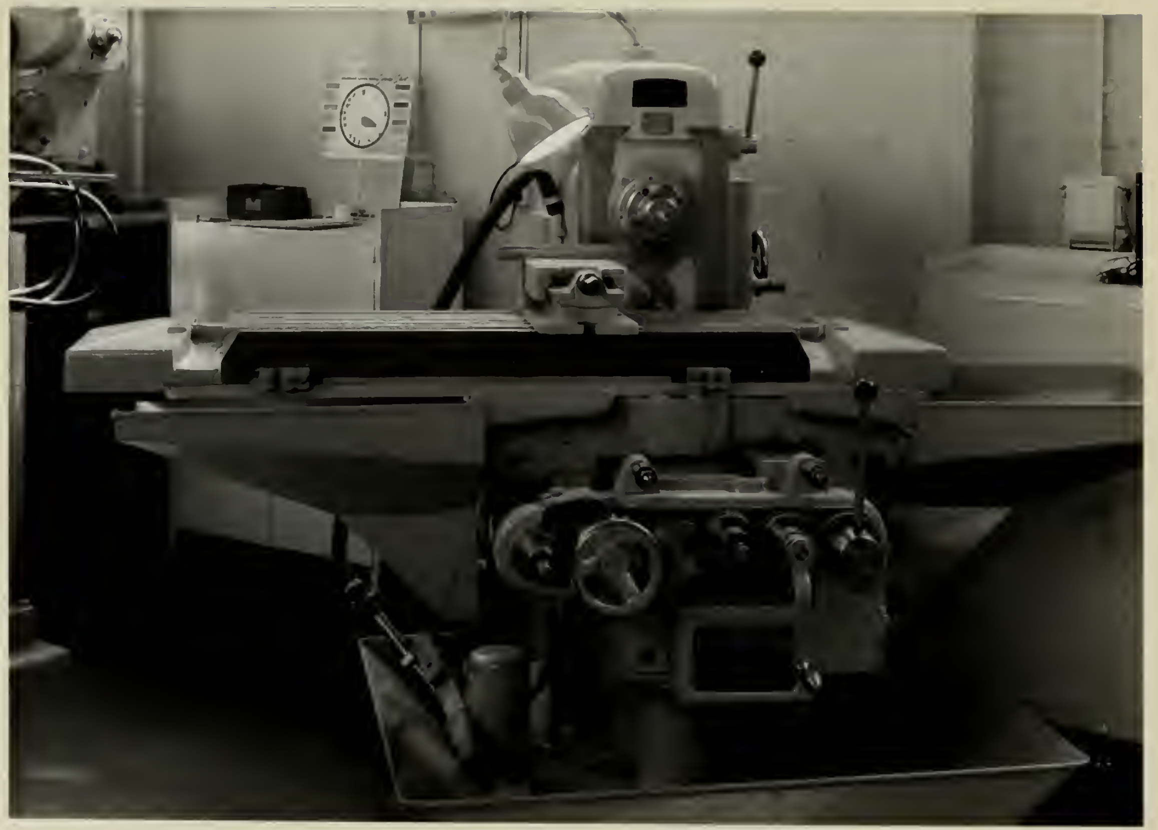
## COMPARATIVE TURNING DATA:

(For DORMER special tool bits cutting DTD 331).

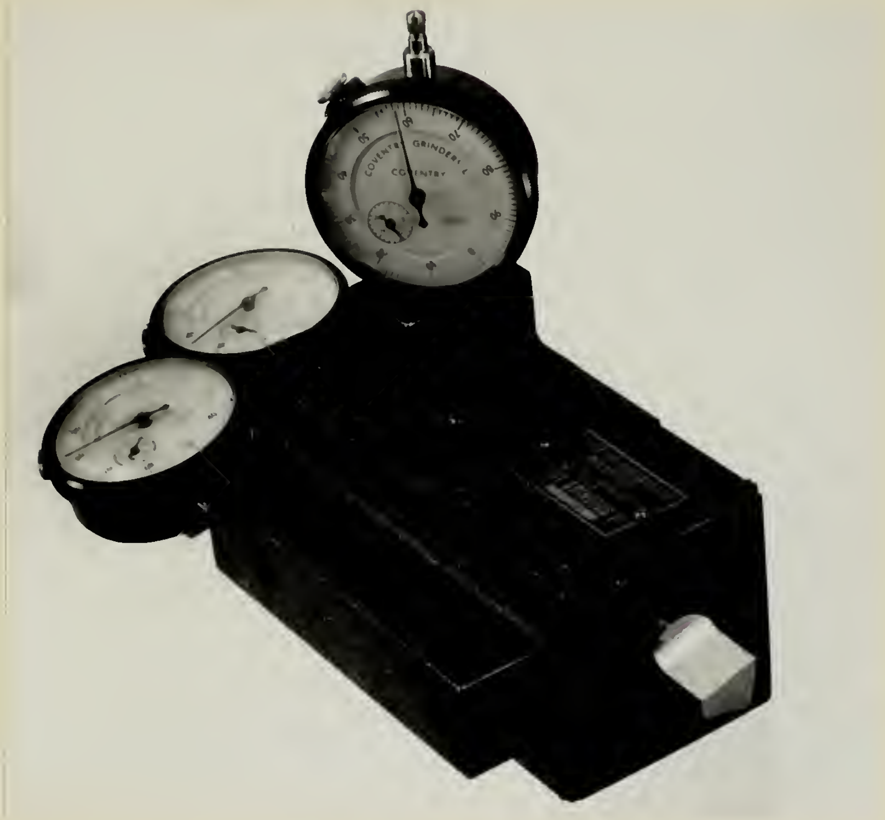
| SIDE RAKE ANGLE | FEED IN/REV | CUTTING SPEED FPM | TOOL LIFE (MIN.) | METAL REMOVED (CU. IN.) |
|---|---|---|---|---|
| 15° | .005 | 100 | .95 | .57 |
| | .005 | 90 | 2.60 | 1.40 |
| | .005 | 80 | 6.10 | 2.93 |
| | .005 | 70 | 17.5 | 7.35 |
| | .0025 | 140 | .60 | .252 |
| | .0025 | 120 | 1.80 | .648 |
| | .0025 | 100 | 9.21 | 2.76 |
| | .0025 | 90 | 23.0 | 6.20 |
| | .010 | 70 | .55 | .46 |
| | .010 | 60 | 1.70 | 1.22 |
| | .010 | 50 | 5.82 | 3.48 |
| | .010 | 40 | 25.3 | 12.12 |



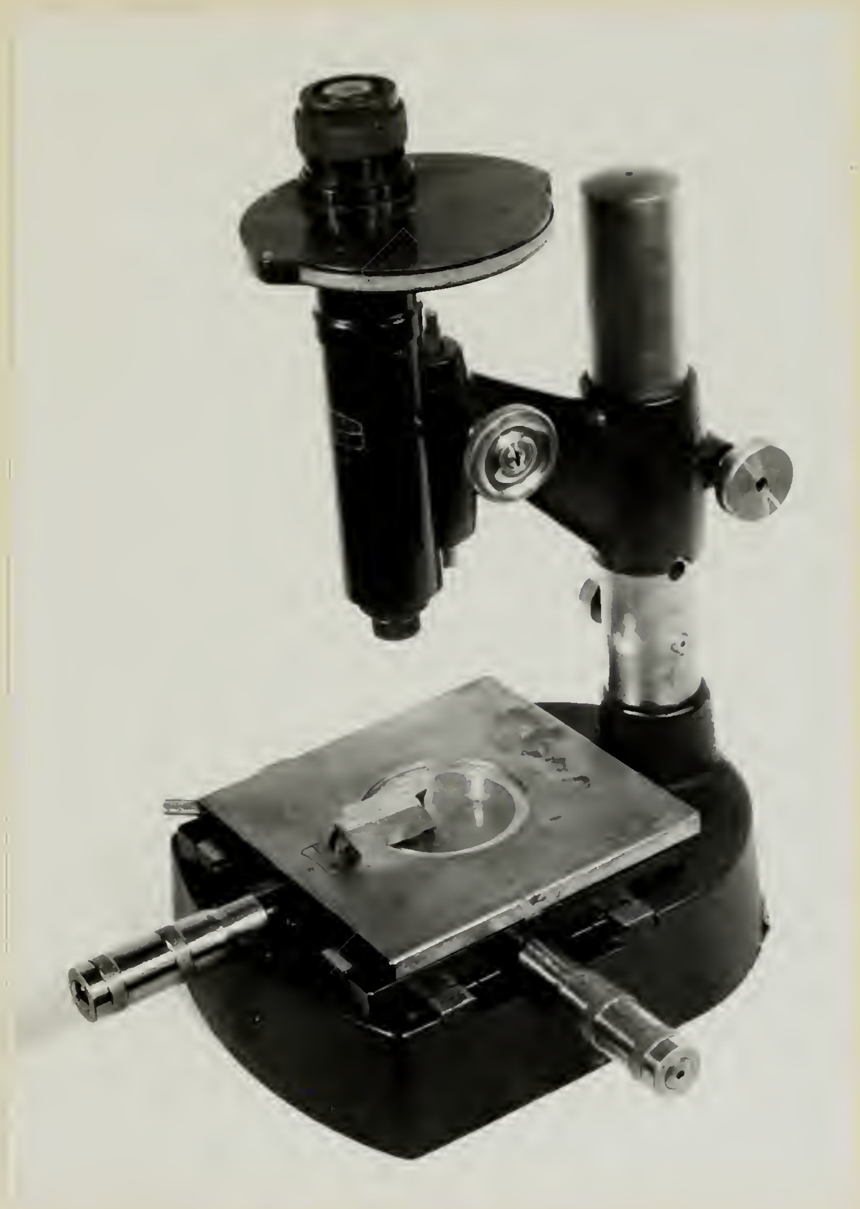
MARTIN LATHE



LOEWE MILLING MACHINE



LATHE TOOL DYNAMOMETER



TOOLMAKERS MICROSCOPE

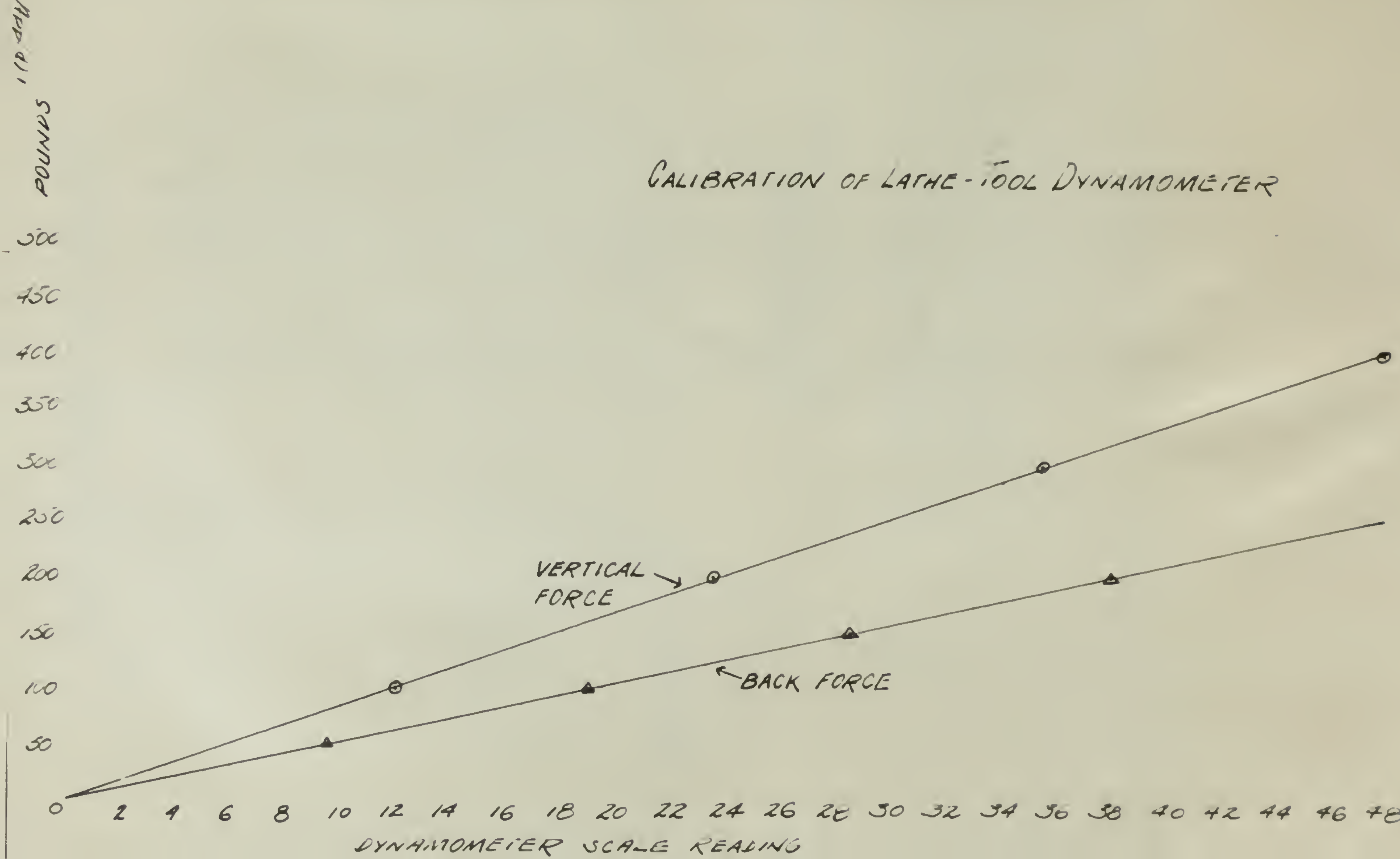
## CALIBRATION OF LATHE TOOL DYNAMOMETER:

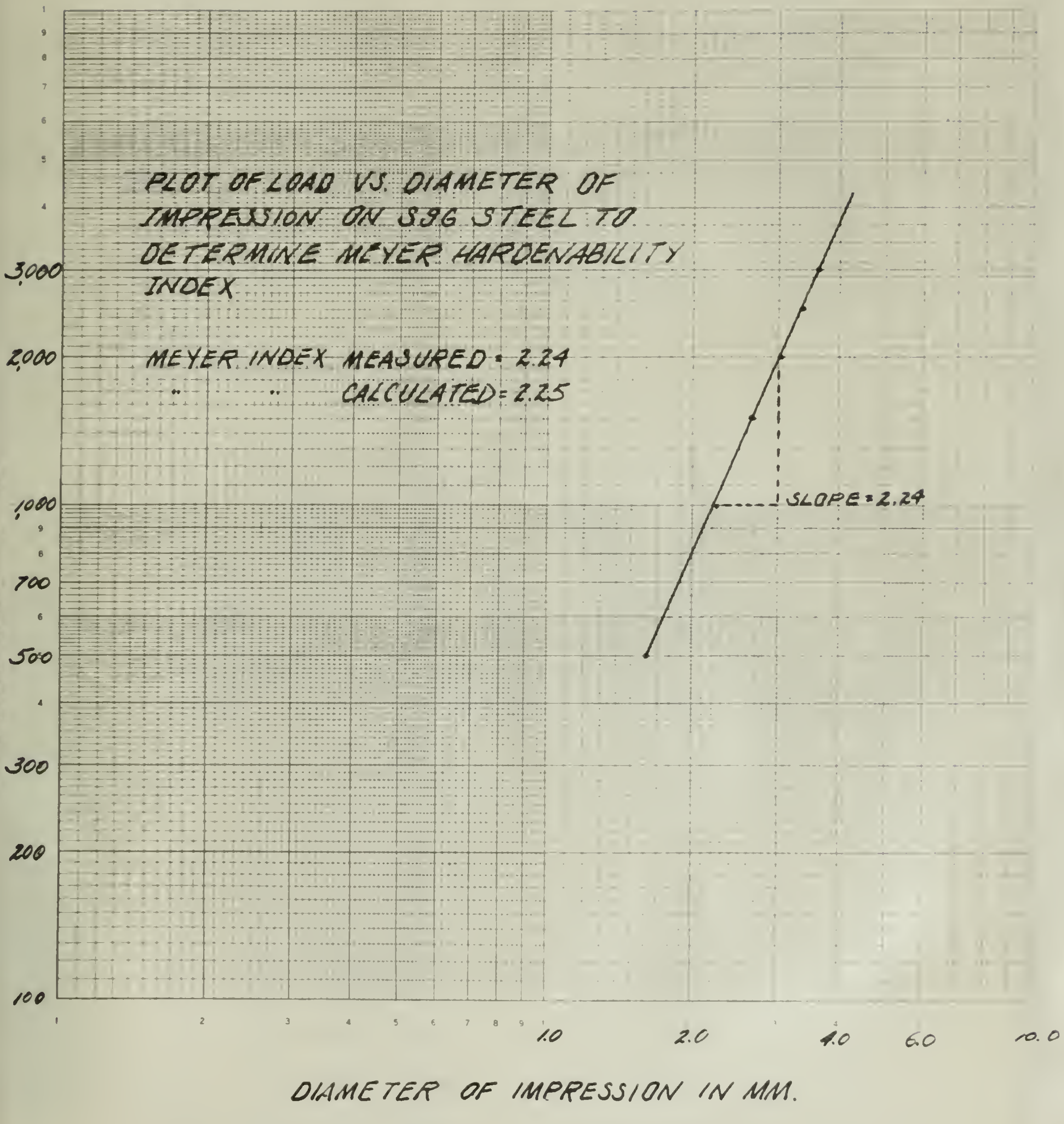
The dynamometer was calibrated by applying known loads by means of the standard College of Aeronautics calibration apparatus and noting the corresponding gauge reading.

| APPLIED LOAD (POUNDS) | AVERAGE GAUGE READING |
|---|---|
| 100 | 11.75 |
| 200 | 23.5 |
| 300 | 35.25 |
| 400 | 47.25 |
| 500 | 59.25 |

App d11

# CALIBRATION OF LATHE-TOOL DYNAMOMETER





PLOT OF LOAD VS. DIAMETER OF IMPRESSION ON S9G STEEL TO DETERMINE MEYER HARDENABILITY INDEX

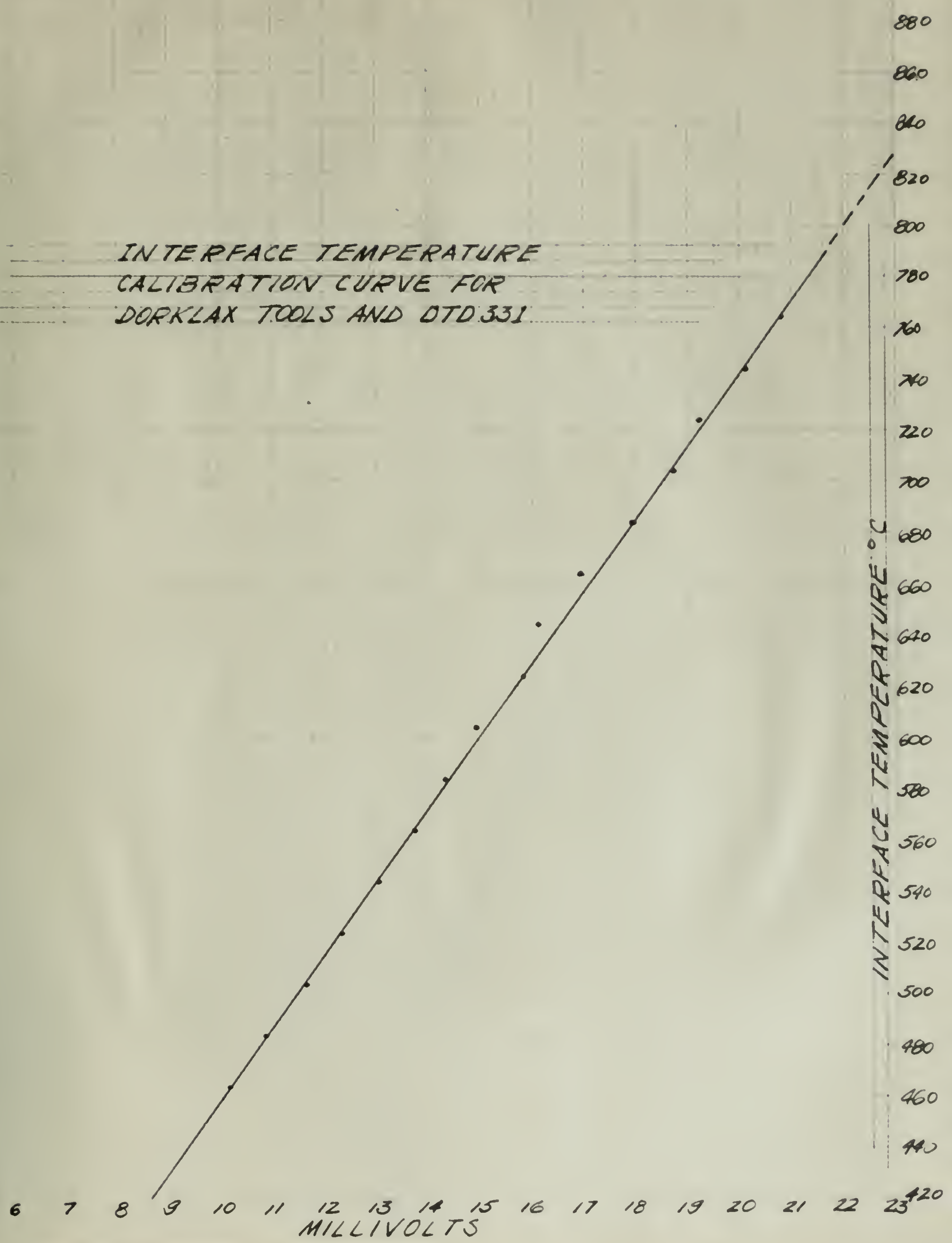
MEYER INDEX MEASURED = 2.24
" " CALCULATED = 2.25

## CALIBRATION OF THERMOCOUPLE

The thermocouple and millivoltmeter circuit was calibrated by the standard method of immersing together the cutting tool and a chip of the workpiece in a lead bath whose temperature was continuously measured by a standard thermocouple and potentiometer. The voltage induced in the tool-workpiece circuit as measured by the millivoltmeter is then associated with the corresponding measured temperature of the lead bath. This is the standard procedure as described in (44) and (9) and other references.

(For DORKLAX tools).

| WORKPIECE MATERIAL | MILLIVOLTMETER SCALE READING (MV.) | LEAD BATH TEMPERATURE °C | WORKPIECE MATERIAL | MILLIVOLTMETER SCALE READING (MV.) | LEAD BATH TEMPERATURE °C |
|---|---|---|---|---|---|
| S-96 | 11.0 | 602 | DTD 331 | 20.8 | 764 |
| | 10.8 | 582 | | 20.1 | 744 |
| | 10.3 | 562 | | 19.2 | 724 |
| | 9.9 | 542 | | 18.7 | 704 |
| | 9.3 | 522 | | 17.9 | 684 |
| | 9.0 | 502 | | 16.9 | 664 |
| | 8.4 | 482 | | 15.8 | 624 |
| | 7.9 | 462 | | 14.3 | 584 |
| | 7.3 | 442 | | 13.0 | 544 |
| | 6.8 | 422 | | 11.6 | 504 |



INTERFACE TEMPERATURE CALIBRATION CURVE FOR DORKLAX TOOLS AND DTD 331

# ACKNOWLEDGMENT

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Wallace C

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